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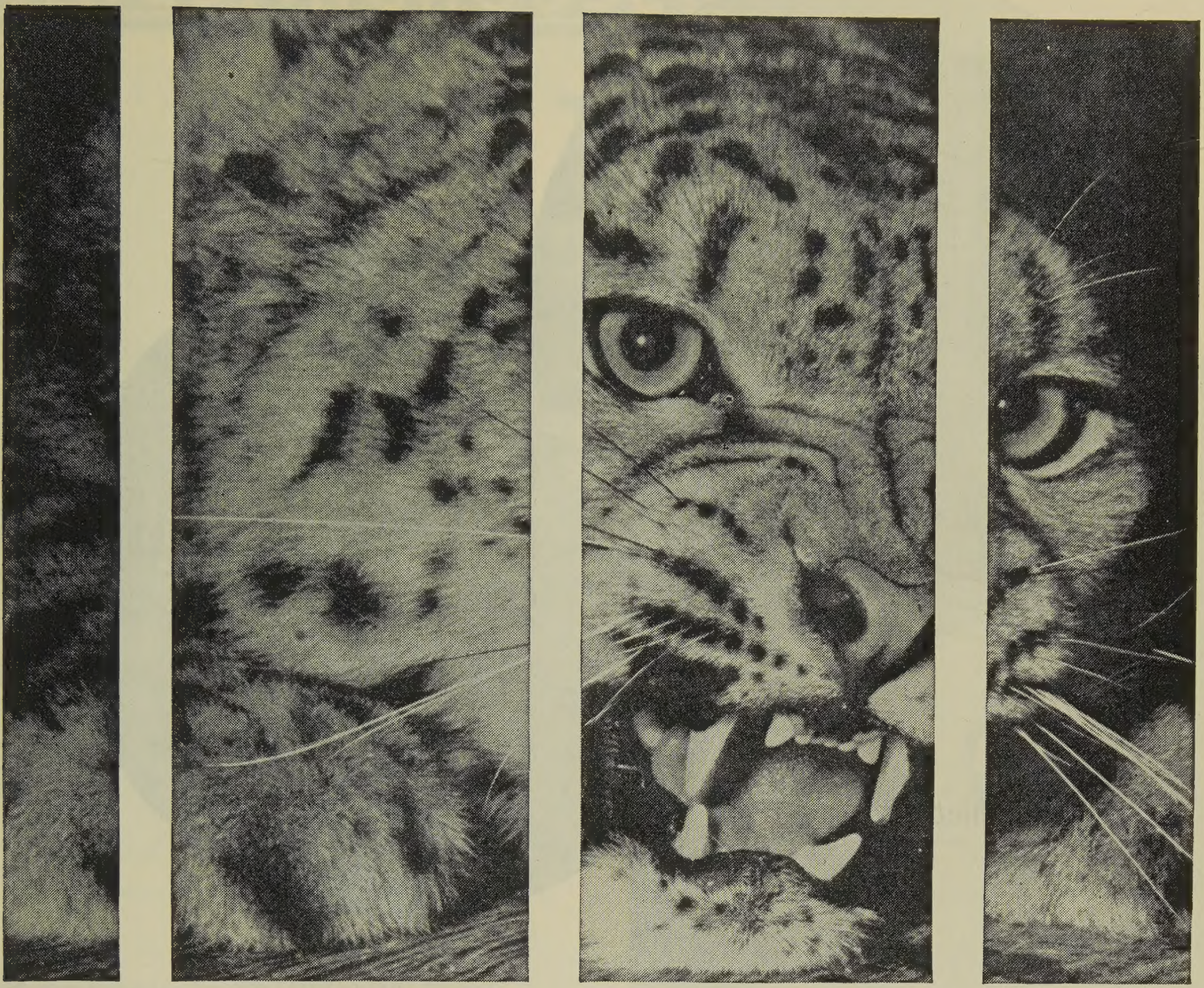

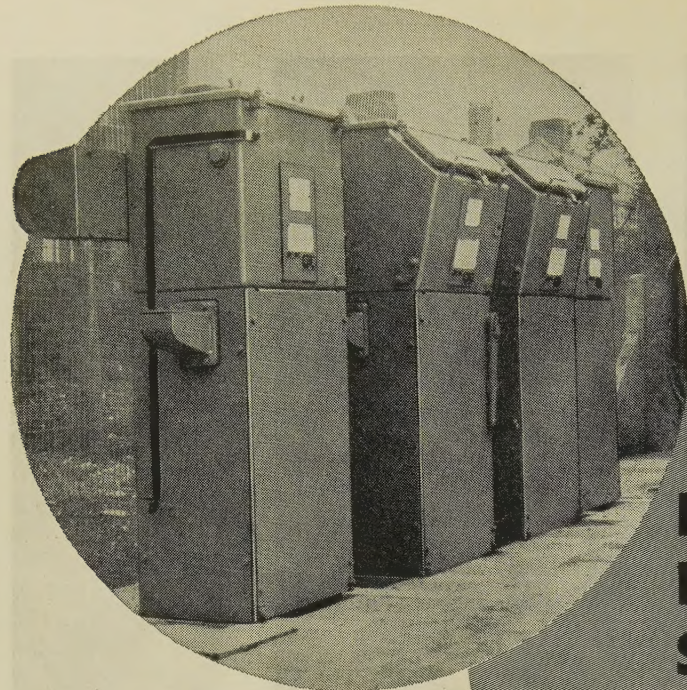

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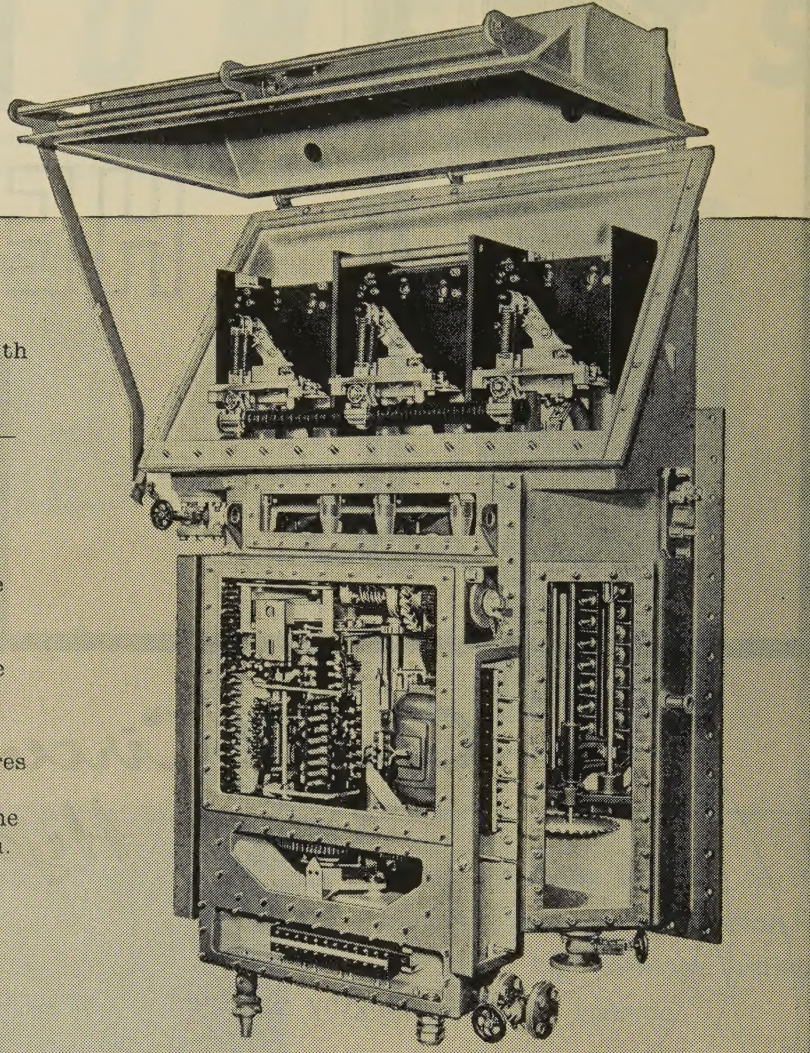

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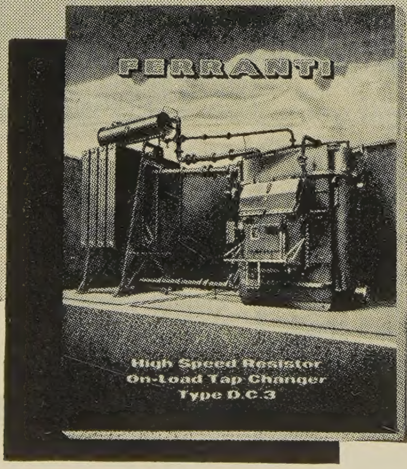

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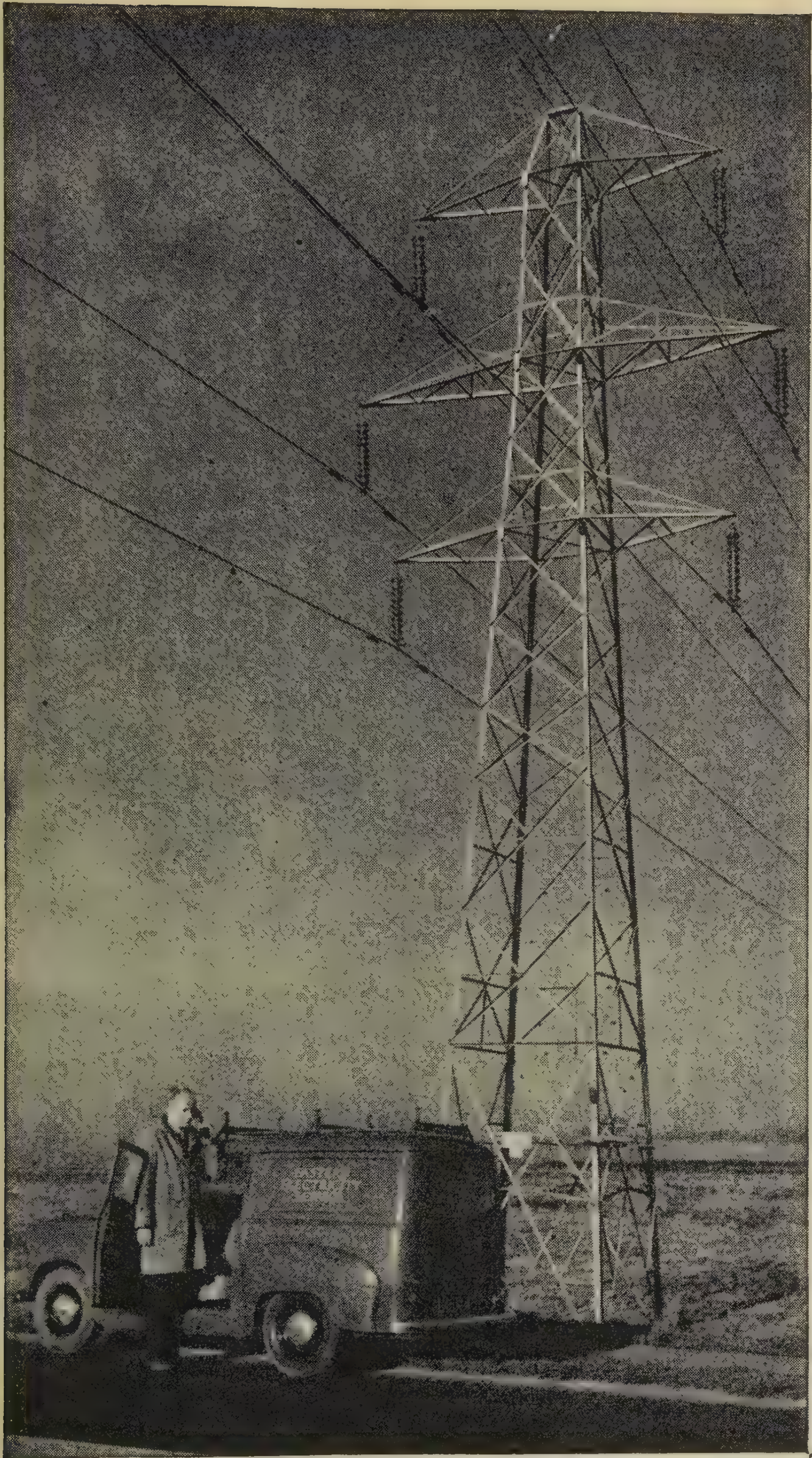

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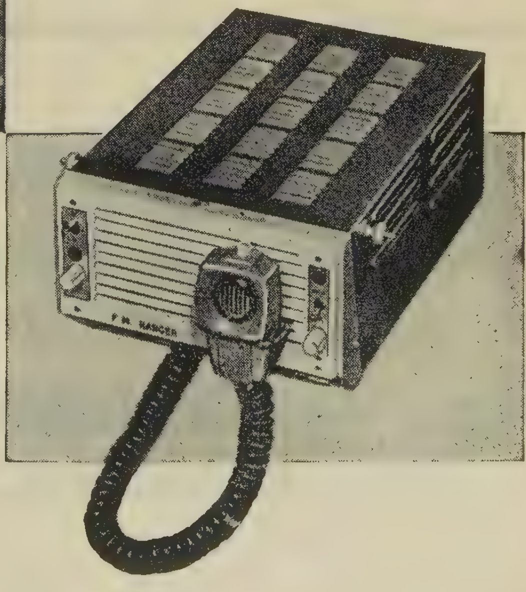

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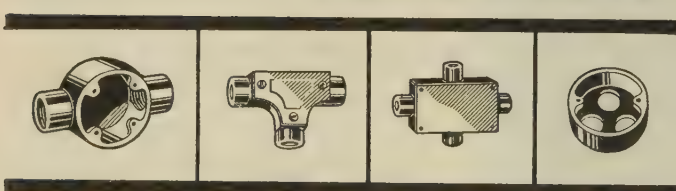

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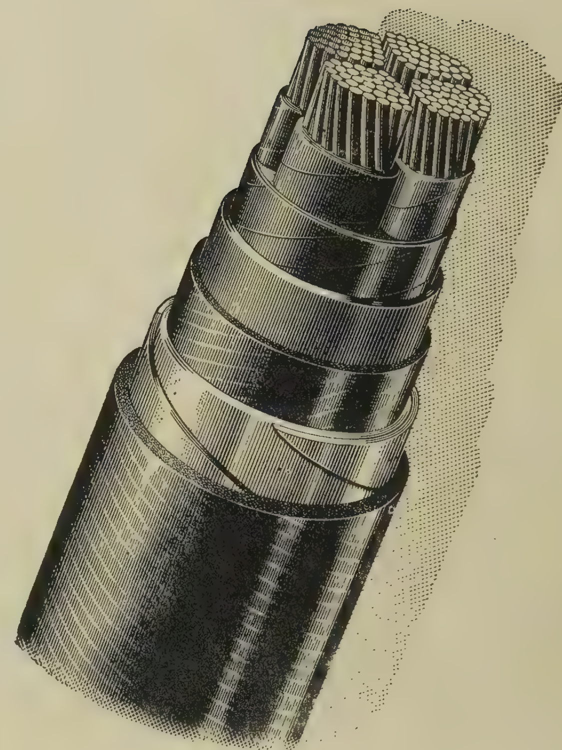

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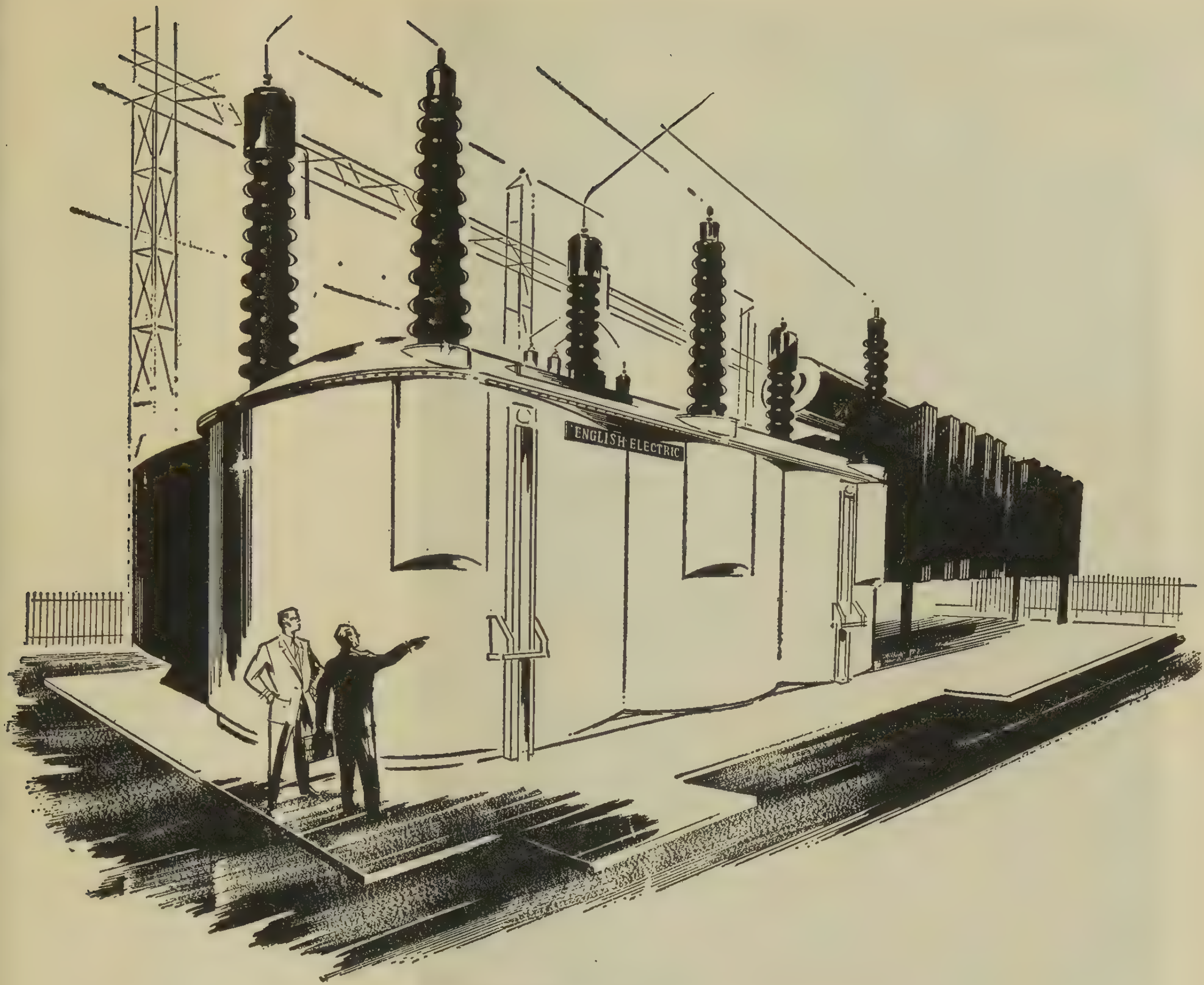
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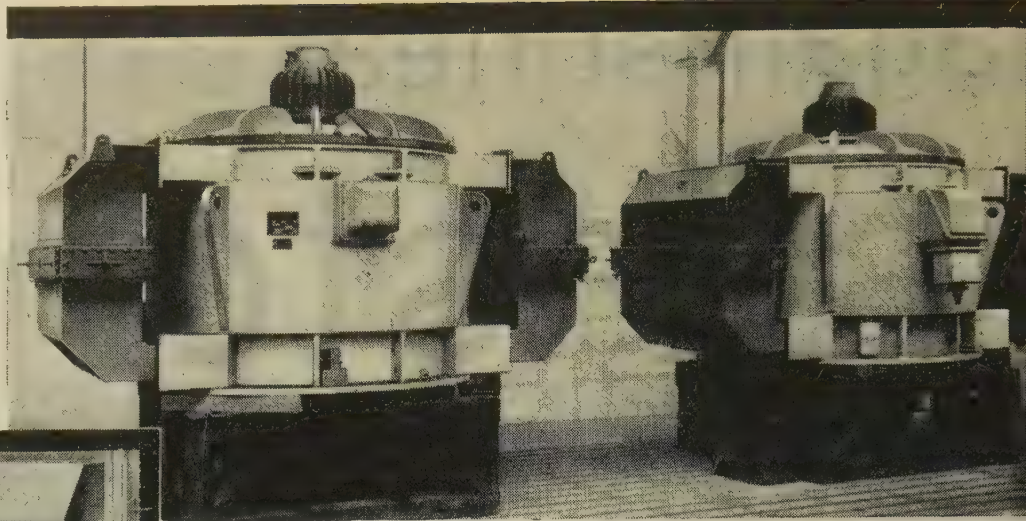

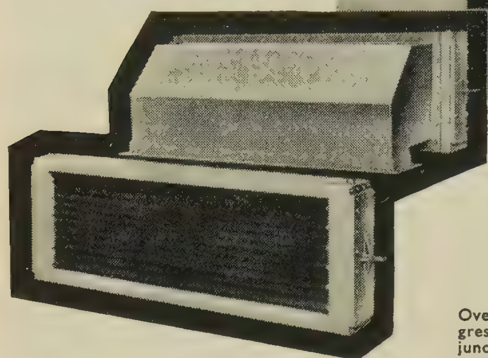

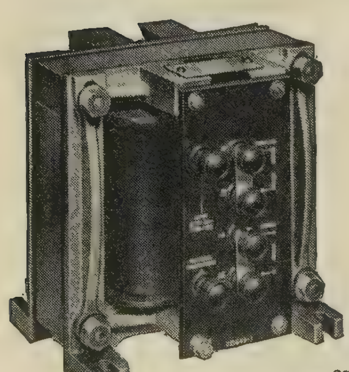

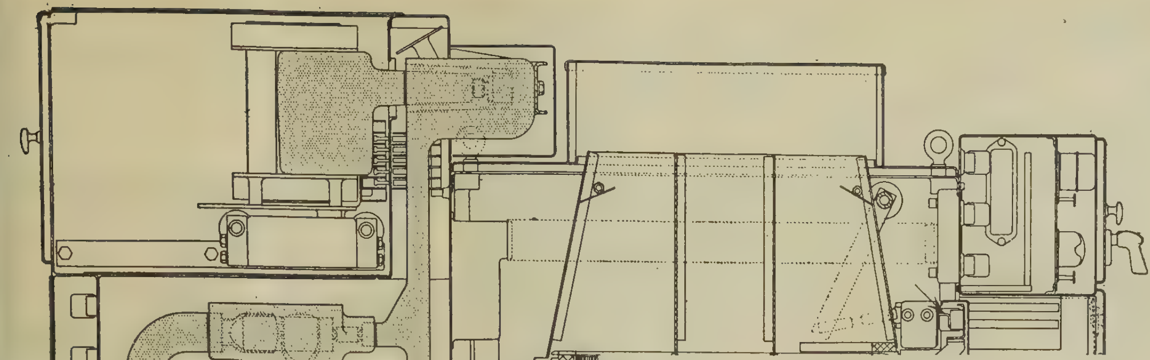

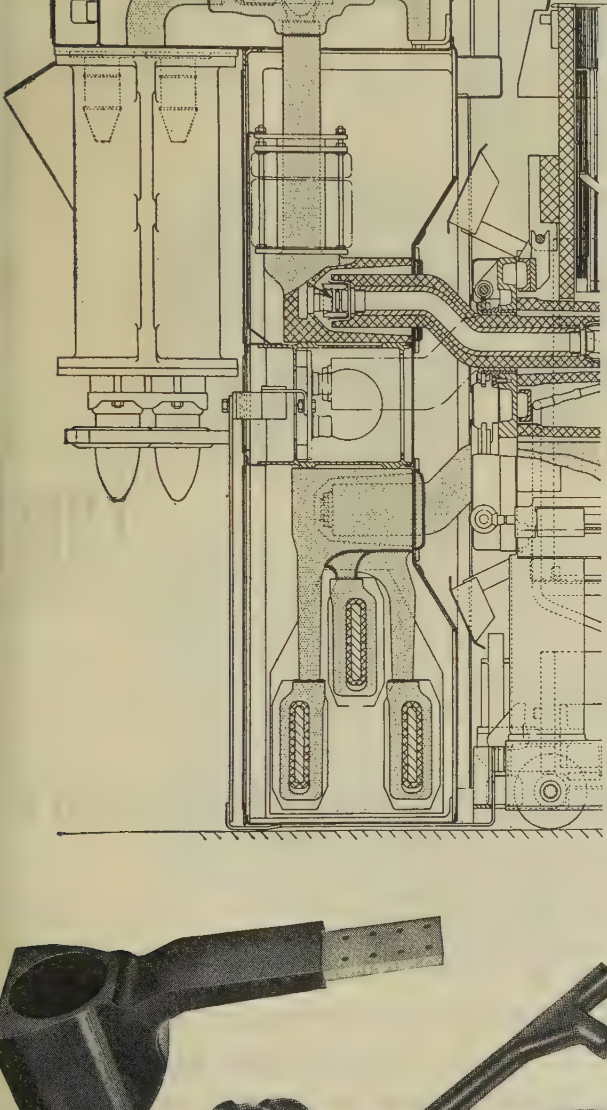

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THE SHIELDING OF OVERHEAD LINES AGAINST LIGHTNING

By J. H. GRIDLEY, Ph.D., B.Sc.(Eng.), Associate Member.

(*The paper was first received* 24*th June, and in revised form* 9*th October*, 1959. *It was published in January*, 1960, *and was read before* THE INSTITUTION 4*th February, and the* NORTH-EASTERN CENTRE 21*st March*, 1960.)

SUMMARY

Most overhead transmission lines carry over-running earth wires, one function of which is to shield phase conductors from direct lightning strokes. Published theories of shielding are incompatible with modern knowledge of lightning. The paper emphasizes the importance of the charges bound on the earth wire and phase conductor by the leader stroke. Equality of charges is taken as a condition under which both wires are equally likely to be struck. For adequate shielding, the charge on the earth wire should exceed that on the conductor. The charges are calculated from the field due to the leader, and it is shown that equality of charges corresponds to both wires lying on the same equipotential of the leader. The limiting position for protection is determined by the slope of the equipotential, and as this depends on the distance between leader and transmission line, the limiting value of which depends on the stroke current, the protection afforded is a function of stroke current. Calculation suggests that shielding is normally adequate for conductors in a wedge of semi-vertical angle 45° of which the apex line is the earth wire.

(1) INTRODUCTION

An early approach to the problem of earth-wire protection conceived the function of the earth wire as that of neutralizing the charge on a thunder cloud by point discharge. To this end, early earth wires were barbed. It was recognized later that the barbs had no value, and the function of an earth wire was considered to be that of reducing the potential induced by a thunder cloud on the phase conductors. It was argued that an isolated conductor would acquire a high potential in the field of a thunder cloud, but that a conductor earthed through transformers, or even by the leakage resistance of the system, would remain at earth potential by acquiring a charge of opposite sign to that in the cloud base. If the inducing cloud charge were abruptly neutralized by a lightning flash (not necessarily to the line, or even to earth) the charge left on the phase conductor would bring it to a high potential. By shielding phase conductors from the influence of a charged cloud, an earth wire reduces the potential induced on them when a flash discharges the cloud. In modern terms this would constitute protection against indirect surges. Although this effect undoubtedly occurs, it is now well established[1] that indirect surges are of importance only on lines operating at voltages of 33 kV and below; earth wires on higher-voltage lines must justify their installation by offering protection against direct strokes.

Bewley[2] derived an expression for the shielding effect of an earth wire on the assumption that lightning struck that earthed object or surface nearest to its point of origin in the cloud. Although this approach was a significant advance, it can be strongly criticized in its neglect of the leader process. A lightning flash to earth is normally preceded by a leader stroke which progresses from cloud to earth through a field which averages hundreds of volts per centimetre. Superposed on this average field are random variations due to atmospheric space charges, and the progress of the leader is often tortuous as its tip is diverted from the average downwards direction.

Prediction of the position of the earth termination of a leader stroke is thus uncertain until the leader channel has covered a major part of the distance between cloud and ground.

Accordingly, Schwaiger[3] assumed that prediction was impossible until the tip of the leader streamer had descended to the level of structures which might possibly be struck. In both Bewley's and Schwaiger's theories the central assumption is that a spark will take the shortest path between two electrodes, but the former considers the upper electrode as being at cloud height and the latter considers it to be at structure height. Although Schwaiger's theory is probably less at variance with modern knowledge than Bewley's, it is also open to serious criticism when viewed against the background of the final stage of the leader process. Golde[4] has argued that when the tip of the leader stroke descends to a certain level the charge distributed on it must cause a field intensity at the surface of the ground, or at an earthed structure, sufficient for an upward streamer from the ground or structure to be formed. The cloud–earth path is completed when the leader from the cloud and the streamer from earth join to form a continuous conducting channel. A theory of the protective effect of an earth wire must take this phenomenon into account and must consider the field distribution created by the leader charges in relation to the

The paper forms part of a thesis submitted for the degree of Doctor of Philosophy at London University.
Dr. Gridley is Head of the Electrical Engineering Department, Regent Street Polytechnic.

occurrence of upward streamers. A purely geometrical approach to find the shortest path between a point representing the tip of the leader and a conductor, and which ignores the field associated with the leader charges, is hard to defend.

Kozelj[5, 6] has analysed the problem of earth-wire protection as an electric-field problem. The electric field due to atmospheric charges is calculated for the region around an earth wire and phase conductor. A knowledge of this field permits the calculation of bound charges on the earth wire and conductor, and Kozelj assumes that the relative magnitudes of these bound charges determine the probability of a stroke to either wire. Although Kozelj does not defend this assumption, arguments can be advanced (Section 2.1) for taking equal bound charges on earth wire and phase conductor to imply equal probability of upward streamers from each. Kozelj's work is accordingly compatible with the modern picture of the later stages of the leader process and represents an advance on purely geometrical theories. On the other hand, the distribution of atmospheric charges assumed by Kozelj is one which would produce a uniform field in the absence of earthed structures. Now a charged leader channel extending from cloud to a point relatively near earth does not produce a uniform field, and while Kozelj has considered the electric field between plane electrodes (cloud and ground) as affected by the presence of the earth wire and conductors, what is needed is a consideration of the electric field between rod (leader) and plane (ground) electrodes in the presence of these wires.

In addition to analytical investigations, tests have been made by exposing scale models of transmission lines to sparks from high-voltage surge generators. Objections to this technique raised by Slepian[7] and Golde[8] are so serious, however, that it would be unwise to place any reliance on the results.

The present paper continues the analytical approach, and the central problem in earth-wire protection is regarded as being the disposition of the earth wire and phase conductor in such a manner that an upward streamer shall occur from the former in preference to the latter.

(2) CONDITIONS FOR THE OCCURRENCE OF UPWARD STREAMERS

(2.1) Bound Charge

As the leader channel approaches earth, the charge distributed along its length creates an electric field which becomes more intense as the distance between leader tip and earth decreases. The earth wires and conductors of an overhead line distort this field, and each conductor may become surrounded with an intense potential gradient. At some critical value the gradient may be enough to cause an upward streamer, and from the instant that this happens the wire concerned is normally committed to receive the discharge. Now the potential gradient around a wire is determined by its own charge distribution and by the charge distribution on the leader channel. It can quickly be shown that the charge distributed along a wire is by far the more important in determining the potential gradient around it. Thus, if the charges on a leader channel would impress a potential V_e at a point occupied by an earth wire, in order to remain at earth potential this wire must carry a linear bound-charge density, q_e, such that

$$V_e + q_e/C_e = 0 \quad . \quad . \quad . \quad . \quad . \quad (1)$$

where C_e is the capacitance to earth per metre of the earth wire. At the wire radius, r, the associated electric force is

$$E_r = \frac{q_e}{2\pi\epsilon_0 r} = -\frac{V_e C_e}{2\pi\epsilon_0 r}$$

and if C_e is calculated as the capacitance per metre of a single horizontal wire suspended at height h_e above a perfectly conducting earth,

$$E_r = -V_e/r \log \frac{2h_e}{r}$$

The total electric force at the earth-wire radius may be obtained by combining the above value with the electric force due to the leader charge alone. Golde[9] has given strong reasons for supposing that the electric force due to the leader alone does not exceed about $0 \cdot 3 \times 10^6$ V/m in the vicinity of an earth wire and conductor, the basis of his argument being that higher values cannot be supported in long gaps. Taking a conductor height of 10m, it follows that V_e may be about 3×10^6 volts, and taking a wire radius of 1 cm, E_r becomes 4×10^8 V/m. Although an electric force of this magnitude could not be supported and corona would ensue around the wire, it is clear that the electric force at the surface of an earth wire or conductor, and for some distance around it, is determined primarily by the bound charge which it carries. If it is accepted that an upward streamer will occur from a wire at some critical condition of surface electric force, it follows that an upward streamer is equally likely to occur from an earth wire or a conductor if both carry the same bound-charge distribution, and that an earth wire will shield a conductor if it is so disposed in relation to the leader channel as to carry a greater bound charge than that on the phase conductor.

(2.2) Position of Earth Wire and Phase Conductor in Relation to the Equipotentials of the Leader-Channel Field

If all electrostatic interaction between the earth wire and a phase conductor could be ignored, and also that between either wire and the remaining phase conductors, the bound charge on the most exposed phase conductor would be given by an expression similar to eqn. (1):

$$V_c + q_c/C_c = 0 \quad . \quad . \quad . \quad . \quad . \quad (2)$$

where V_c is the potential which the leader stroke would impress at a point occupied by the phase conductor, C_c is the capacitance to earth per metre of the conductor, and q_c is the linear bound-charge density which it acquires to remain at earth potential. If eqns. (1) and (2) are to be used to determine the bound charges on the earth wire and the most exposed phase conductor, it must first be established that electrostatic interaction between the conductors is negligible and, further, that both remain near earth potential during the development of the leader.

The question of electrostatic interaction will be considered initially in relation to an earth wire disposed to shield a single conductor. In Fig. 1 an earth wire and phase conductor are shown lying on the same equipotential, V, due to the field of charges on the leader channel. The charges which each will acquire will be approximately equal, ignoring the slight difference between capacitances to earth, if neither wire impresses a significant potential at the point occupied by the other, or if each wire impresses the same potential at the point occupied by the other. In the former case the charges on each will be equal since they will be VC_e or VC_c with $C_c \simeq C_e$; in the latter case the charges will be equal, since the impressed potential at the earth-wire position is changed by the presence of the phase conductor by an amount equal to that by which the impressed potential at the conductor position is changed by the presence of the earth wire. It is, in fact, the latter case which is most relevant. As most leader channels are negatively charged, the bound charges on both earth wire and phase conductor will be positive. The earth-wire charge tends to raise the potential at

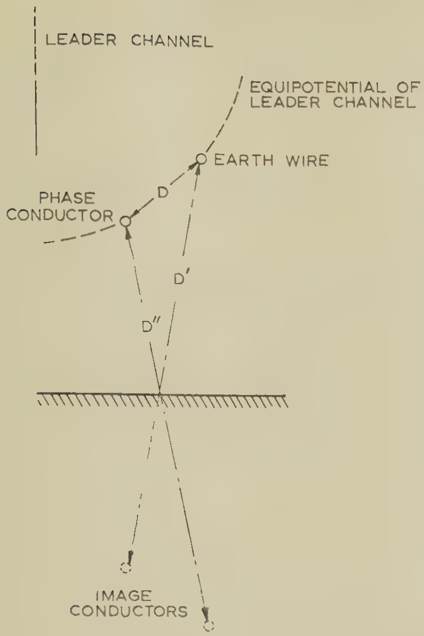

Fig. 1.—Earth wire and phase conductor on equipotential of leader channel.

the phase conductor, and the charge on the phase conductor tends to raise the potential at the earth wire. Instead of the charges adopted for the maintenance of earth potential at each wire being given by $q_c = -VC_c$ and $q_e = -VC_e$, we have

$$q_c = -(V + V_{ec})C_c$$

and

$$q_e = -(V + V_{ce})C_e$$

where V_{ec} is the potential impressed on the phase conductor by the earth-wire charge and V_{ce} is that impressed on the earth wire by the phase conductor charge. Now $V_{ec} = \dfrac{q_e}{2\pi\varepsilon_0} \log \dfrac{D''}{D}$ and $V_{ce} = \dfrac{q_c}{2\pi\varepsilon_0} \log \dfrac{D'}{D}$. With normal tower construction the difference between $\log D'/D$ and $\log D''/D$ does not amount to more than a small percentage. It follows that, if an earth wire and a phase conductor are so disposed as to acquire equal bound charges when the charge on the leader stroke is counted as the only inducing charge, the equality of the charges will still be maintained when allowance is made for the interaction between the two wires.

The above argument has been based on a consideration of an earth wire and a single phase conductor, namely that conductor in the most exposed position. In general there will be at least two other phase conductors, and probably five. Once again, however, the equality of bound charge on an earth wire and a most exposed phase conductor due to their lying on the same equipotential of the leader-channel field will be disturbed only if the remaining conductors impress a potential on the earth wire which differs from that on the most exposed conductor. As an example, the dimensions of a typical 33 kV double-circuit line are shown in Fig. 2. If the phase conductor, A_y, and the earth wire, O, lie on the same equipotential of the leader-channel field, they will bear significantly different bound charges only if the charges bound on the remaining conductors impress a potential on A_y appreciably different from that impressed on O. If the equipotential of the leader-channel field passing through A_y and O has a value V, the charge per metre acquired by A_y and O in the absence of the other conductors will be

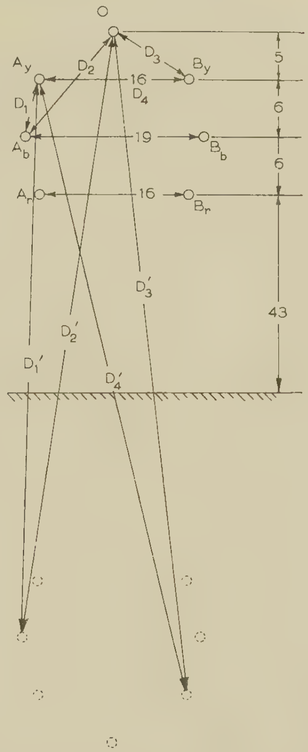

Fig. 2.—Arrangement of earth wire and conductors of 33 kV line.
Conductor radius, 0·24 in; other dimensions in feet.

$q \simeq 2\pi\epsilon_0 V/\log \dfrac{2h}{r}$, where h is the average height of A_y and O, and r is the radius of either wire, assumed equal. Now if all other conductors are assumed to carry the same charge (a conservative assumption, since, in fact, they must lie on equipotentials of lower voltage) the potential difference impressed between A_y and O by the nearest remaining conductors, A_b and B_y, is given as a fraction of V by

$$\frac{\delta V}{V} = \frac{\left(\log \dfrac{D_1'}{D_1} - \log \dfrac{D_2'}{D_2} - \log \dfrac{D_3'}{D_3} + \log \dfrac{D_4'}{D_4}\right)}{\log \dfrac{2h}{r}}$$

This difference, for the line illustrated, is less than 1% and the remaining conductors have an even smaller effect.

To the degree of accuracy at which it is reasonable to calculate in problems connected with lightning, an earth wire may be taken as shielding a conductor if it carries a greater bound charge. The limiting condition for shielding will occur when the earth wire and phase conductor lie on the same equipotential of the field due to the charges on the leader channel, since in this position they carry equal bound charges.

(2.3) The Potential of Phase Conductors during the Leader Process

The application of eqn. (2) to the calculation of the bound charge on phase conductors contains the implicit assumption that phase conductors are near earth potential throughout the leader process. There can be little doubt that an earth wire, earthed at each supporting structure, remains at earth potential during the leader process, but it is not obvious that phase conductors earthed only through remote transformers can be regarded as being at earth potential. First, however, it should

be observed that the normal power-frequency potential on phase conductors can hardly affect the accuracy of the equation. Considering once again an average electric force of the order of $0 \cdot 3 \times 10^6$ V/m between leader channel and a wire about to initiate a streamer, the potential V which would exist at a point occupied by a wire 10m above earth, if the wire were isolated, might amount to 3×10^6 volts. The power-frequency potential of even a 275kV line is less than a tenth of this, and a line of this voltage would almost certainly be strung with the most exposed conductor much higher than 10m. The power-frequency potential can thus be neglected in calculating the bound charge.

For a phase conductor to remain near earth potential during the leader process it must draw charge, ultimately from earth. If the nearest earthed transformer is a considerable distance from the section of line near a downcoming leader stroke, the charge required must be drawn in the first instance from neighbouring sections of the phase conductor. The question then is whether charge can be drawn from the neighbourhood of the affected section fast enough to maintain the affected section at earth potential, despite an increasing impressed potential due to the downcoming leader. The situation can be viewed in a slightly different manner by considering that, as a potential is induced on a section of line by the downcoming leader, it travels away at the propagation velocity for the line; the decay of potential on the section of line near the leader due to waves travelling away from the section corresponds, on the previous viewpoint, to the flow of charge to the affected section. The question may be resolved by considering the relative velocities of leader-channel development and wave propagation on overhead lines; the latter exceeds the former by a factor of about 10^3. It should be recalled that the velocity of the final stage of the leader process, in which junction is made with an upward streamer, is not relevant here; by the time an upward streamer occurs the question whether the earth wire will shield the conductor has been settled and the above argument must apply to earlier stages. Although a full calculation[10] can be carried out to demonstrate the low value of residual potential with a ratio of leader-stroke velocity to wave velocity of the order quoted, it is apparent that the potential which can remain on a phase conductor cannot be appreciable compared with the potential impressed by the leader charges.

It may also be observed that, if a fraction of the charge needed to maintain a phase conductor at earth potential during the development of a leader channel were not available, a numerically equal fraction of the impressed potential due to the leader would appear on the conductor. Thus, if 10% of the charge needed to satisfy eqn. (2) were not available, 10% of V_c would appear on the conductor. But V_c may well be $6 \cdot 0 \times 10^6$ volts, in which case the residual potential would be 600kV. Unless eqn. (2) were satisfied to an accuracy better than 10%, all 11kV lines and most 33kV lines would suffer flashover, simply owing to the development of a nearby leader, and this is certainly not compatible either with experience or theory.[1]

(3) THE SHIELDING EFFECT OF AN EARTH WIRE AS DETERMINED BY THE EQUIPOTENTIALS OF THE LEADER CHANNEL FIELD

(3.1) Configuration of Equipotentials of a Leader Channel

If the reasoning of the previous Sections is correct, the limiting position for an earth wire to shield a conductor corresponds to both wires lying on the same equipotential of the field due to the leader channel. It is therefore important to determine the shapes of these equipotentials.

If the linear charge density on a vertical leader channel with earth tip at height h_0 and extending to height H is a function, $q(h)$, of height, the potential at a point x metres distant from the channel and y metres above earth is

$$V_{(xy)} = \frac{1}{4\pi\epsilon_0}\int_{h_0}^{H}\left\{\frac{q(h)}{\sqrt{[(h-y)^2+x^2]}} - \frac{q(h)}{\sqrt{[(h+y)^2+x^2]}}\right\}dh \quad . \quad . \quad . \quad . \quad (3)$$

The first term of the integrand corresponds to the charge distribution on the leader channel, the second to the image-charge distribution.

Now the current in a return stroke neutralizes the charge on the leader channel. Bruce and Golde[11] have calculated the distribution of leader-stroke charge to account for the observed waveform of return-stroke current and conclude that the describing function for leader-stroke charge is of the form $q(h) = q_0\varepsilon^{-kh}$, where q_0 is the charge per metre length at the tip of the leader channel. As the average value of k which they suggest is about $1 \cdot 0 \times 10^{-3}$, sufficient accuracy is obtained for small values of h if the charge distribution is written $q(h) = q_0(1 - kh)$.

Further, it is soon shown that the charge in the upper reaches of the leader channel, say beyond the first 200m, has only a minor effect on the potential at the earth's surface. Then the upper limit of integration can be set at this value instead of a rather uncertain one corresponding to cloud height. Eqn. (3) then becomes

$$V_{(x,y)} = \frac{q_0}{4\pi\epsilon_0}\int_{h_0}^{H}\left\{\frac{(1-kh)}{\sqrt{[(h-y)^2+x^2]}} - \frac{(1-kh)}{\sqrt{[(h+y)^2+x^2]}}\right\}dh$$

and on integration,

$$V_{(x,y)} = \frac{q_0}{4\pi\epsilon_0}\left\{\log\frac{[\delta+\sqrt{(\delta^2+1)}][\beta+\sqrt{(\beta^2+1)}]}{[\alpha+\sqrt{(\alpha^2+1)}][\gamma+\sqrt{(\gamma^2+1)}]}\right.$$
$$- ky\log\frac{[\delta+\sqrt{(\delta^2+1)}][\gamma+\sqrt{(\gamma^2+1)}]}{[\alpha+\sqrt{(\alpha^2+1)}][\beta+\sqrt{(\beta^2+1)}]}$$
$$\left. + kx[\sqrt{(\gamma^2+1)}+\sqrt{(\alpha^2+1)}-\sqrt{(\delta^2+1)}-\sqrt{(\beta^2+1)}]\right\}$$

where $\alpha = (h_0 - y)/x$, $\beta = (h_0 + y)/x$, $\delta = (H - y)/x$ and $\gamma = (H + y)/x$.

An average value for q_0 suggested by Bruce and Golde[11] is 10^{-3} C/m; the value of h_0, the height of the earth tip of the leader at which upward streamers may be expected from earth, has been calculated by Golde[4] as about 17m for this value of q_0. Using these values, the magnitude of $V(x, y)$ has been determined for a region around a leader channel and, by interpolation, equipotentials have been inserted. These are illustrated in Fig. 3.

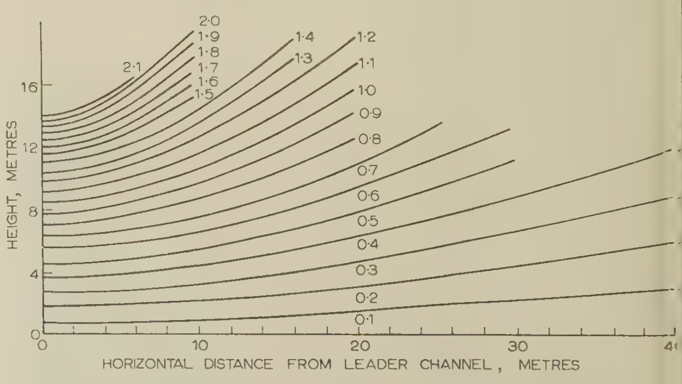

Fig. 3.—Equipotentials of electric field of leader channel.

Potentials shown are multiples of $q_0/4\pi\epsilon_0$ and can be converted to megavolts for $q_0 = 10^{-3}$ C/m by multiplication by $8 \cdot 98$.

(3.2) Determination of Shielding Effect from Equipotential Plots

The question whether an earth wire is in a position to shield a phase conductor can now be determined with the aid of these equipotentials. It should first be noted that, if a leader channel with the above average value of q_0 develops some distance away from an overhead transmission line, unless a streamer occurs either from the earth wire or phase conductor at a leader-tip height more than 17 m the question of shielding does not arise, for, when the leader tip descends further, upward streamers can be expected from the surface of the earth and no part of the line will be struck. If, when the leader tip is at 17 m or higher, the equipotential passing through the earth wire is of higher potential than that passing through the phase conductor, and is above a certain critical value, the earth wire will send out an upward streamer to make contact with the leader channel and so shield the phase conductors. Equally, if one phase conductor lies on a higher equipotential than the earth wire, a streamer will arise from it and the shielding will fail. The limiting condition occurs when the earth wire and most exposed phase conductor lie on the same equipotential.

In order to make use of equipotential plots such as those in Fig. 3, further information is required. First the minimum distance between leader and transmission line at which the line is safe from a direct stroke must be known, since it is clearly of no value to calculate an effective position for an earth wire in relation to a leader which will not make direct contact with either earth wire or phase conductor. Secondly, Fig. 3 has been drawn for a leader-tip height of 17 m, for this is the lowest level to which a leader with $q_0 = 10^{-3}$ C/m may be expected to descend without upward streamers from earth removing the danger of a direct stroke to the transmission line. As this value of q_0 is only an average, the distance of 17 m is also an average, and the effective dispositions of earth wire and conductor must be investigated for various heights of leader tip.

It might be expected that the maximum distance between a developing leader and a transmission line at which the line is exposed would be a function of the charge on the leader channel, and that the lowest level to which the tip of a leader stroke might descend without causing upward streamers from earth would also be a function of this charge. An analysis of the electric field at the surface of the ground shows this to be the case, and Golde[9, 12] has suggested curves for the probable forms of the functions in question. These curves, with slight modifications,[10] have been used in the paper.

Calculation of effective dispositions of earth wire and phase conductor then proceeds as follows. For a chosen value of leader-channel charge the maximum distance between leader and line at which the line is exposed to direct strokes is calculated; also, the lowest level to which the tip of the leader channel may descend without causing upward streamers from the earth is calculated. Equipotentials for a leader channel with tip distance by these amounts from a transmission line are constructed, and the limiting position for the earth wire to shield a phase conductor is determined. This can most conveniently be carried out by a change in the scale of Fig. 3. The efficacy of an earth wire is conveniently expressed as a protective angle, i.e. that angle made by a vertical through the earth wire and a line joining earth wire and phase conductor when the latter is at the limiting position for shielding. This angle may be derived with sufficient accuracy from the slope of the equipotential passing through the earth wire. The procedure is then repeated for further values of leader charge.

Fig. 4 shows the variation of the protective angle of an earth wire with leader charge for different earth-wire heights. If the peak value of return-stroke current is taken as proportional to the charge on the leader channel[11] an alternative current scale can be used.

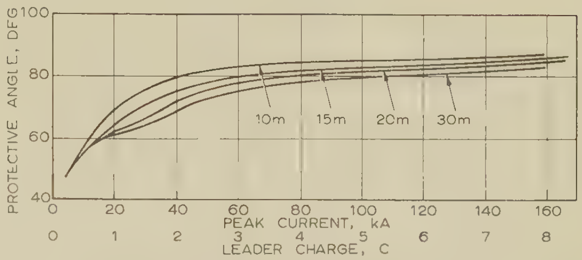

Fig. 4.—Variation of protective angle with charge on leader channel or peak value of return-stroke current.

For a transmission line with fixed construction the earth wire may or may not shield, depending on whether the most exposed phase conductor lies inside or outside the protective angle for the leader charge involved. Fig. 5 shows the frequency of occurrence of lightning currents with specified peak magnitudes,[11] and from this, Fig. 6 has been constructed, showing the percentage of strokes against which protection is afforded for a fixed angle between earth wire and conductor.

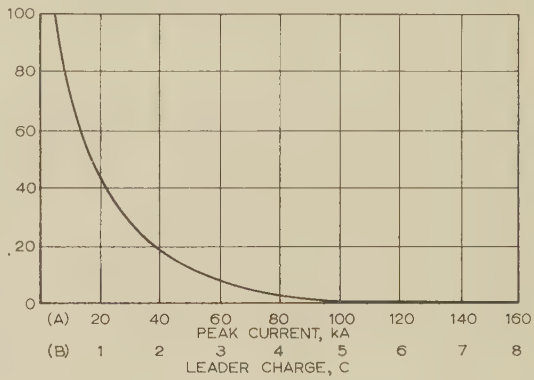

Fig. 5.—Frequency of occurrence of stroke currents.

Vertical scale shows percentage of strokes with peak current exceeding that on horizontal scale A. Scale B is leader charge associated with corresponding peak current.

(4) THE SHIELDING EFFECT IN RELATION TO STROKE CURRENT

The curves of Fig. 6 show that an earth-wire/phase-conductor angle of 88°, i.e. earth wire and phase conductor nearly in the same horizontal plane, gives no protection to the phase conductor against direct strokes. An earth wire installed to make an angle of about 48° with a phase conductor appears to give full protection. Unfortunately both limits are functions of our knowledge of the peak values of lightning currents. Fig. 5 shows that no lightning current exceeds 160 kA, since, at the date of its publication, no higher unambiguous value had been recorded. Accordingly, Fig. 6 shows that an earth wire only capable of shielding against currents greater than 160 kA gives no protection. Similarly, 48° appears as a wholly safe angle, since it is safe for currents greater than 5 kA, the lowest limit of recording attainable with magnetic links. The upper current limit of 160 kA and the corresponding angle of 88° are of little importance in the present context, since earth wires are normally installed to make angles with phase conductors much smaller than 88° and strokes of peak current above 160 kA would therefore, on the basis of the above theory, be intercepted. On the other hand, if the range of lightning currents extended well

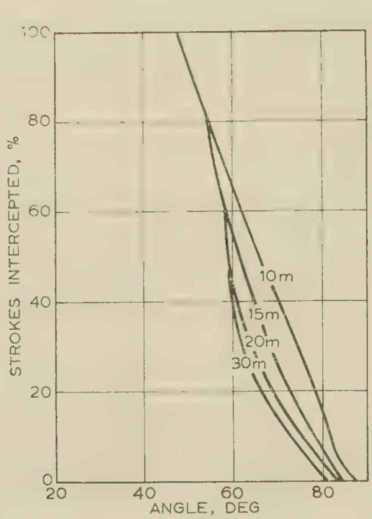

Fig. 6.—Percentage of strokes against which protection is afforded, plotted against earth-wire/conductor angle.

below 5 kA, it would imply that complete protection dictated an earth wire almost vertically above the conductors. The problem of shielding against strokes of low peak current must be considered in relation to the impulse level of the line insulation; it is not necessary to install an earth wire in such a manner that it intercepts strokes which would not in any case endanger line insulation. Thus, for a transmission line with an impulse level of 500 kV the minimum current against which protection is required is about 2 kA, and the corresponding angle is about 45°. The present theory indicates that such an angle would therefore give protection against all dangerous strokes on most h.v. lines.

There is some possibility of shielding failure if the leader stroke is slanted away from the line,[10] but the slant must be continued to quite low levels if shielding is to fail, and it might be expected that the leader stroke would progress in a more or less vertical direction in the closing stages of its travel.

(5) EXPERIENCE WITH OVER-RUNNING EARTH WIRES

For a complete comparison of the foregoing theory with operating experience, records of transmission lines before and after the installation of earth wires making various angles with conductors would be needed. Although such records are available,[13, 14, 15] in nearly all of those examined the earth wire was installed in a manner which would correspond, on the above theory, to almost complete interception of direct strokes, and the only conclusion possible from the resulting improvement in performance is that the angles suggested above are not incompatible with experience, although they cannot be directly confirmed by the available data.

The closest correspondence between the present theory and practice is in the data of the Wallenpaupack-Siegfried 220 kV line,[14] where the installation of an earth wire at an angle of 44–47° to the most exposed conductor reduced the average number of flashovers at towers from 74 per 100 miles per year to 20 per 100 miles per year. As improved earthing then reduced the rate to nearly zero it can be assumed that the earth wire was completely effective in shielding the phase conductors and that the residual fault rate was due to back flashover.

Experience in the U.S.S.R. is summarized by Burgsdorf,[16] who arrives at the conclusion that the probability of a direct stroke to a phase conductor, expressed as a percentage of the total number of lightning strokes to a line, increases from about 0·06% for an earth-wire/conductor angle of 20° to about 2% at angles between 40° and 45°. The exact procedure adopted for discriminating between flashovers due to direct strokes and those due to strokes to towers resulting in back flashover is not given, but Burgsdorf remarks that 'Direct strokes to conductors may embrace flashover of the insulators of the top conductors . . . and interruptions on 150–220 kV transmission lines with low earthing resistance (less than 5 ohms) where the probability of back flashovers is extremely small'. Back flashovers 'take in, for example, flashover of the insulator chains of the middle and lower conductor, earth fault on a number of phases and so on'. Now flashover of a top phase insulator alone cannot be regarded as evidence of a direct stroke. A disproportionate number of flashovers on top phase insulation could be regarded as evidence of shielding failure, but the division of flashovers between phases is not given. Again, as no conclusive evidence is available concerning the importance of the inductive drop in tower structures, and as this may well be important for towers of low footing resistance, care must be taken in concluding that shielding failure accounts for all flashovers on towers of low footing resistance. It is likely that, if the summary of experience in the U.S.S.R. is at all in error, it errs in taking too pessimistic a view of the shielding effect of an earth wire. As the estimate of 2% shielding failures for an earth-wire/conductor angle of about 45° is based on an estimate of only 18 interruptions ascribed to direct strokes to phase conductors, and the estimate of 0·9% to 1·8% failures at 35°–40° based on about three estimated direct strokes, quite small numbers can seriously affect the issue.

Baatz[17] reports statistics for overhead lines in Germany in the voltage range 220–20 kV, the survey having been carried out over 2000 km of line during the years 1937–40. Out of 25 records ascribed to direct strokes, only three refer to an earth wire/conductor angle of less than 45°; the remainder refer to lines with earth-wire/conductor angles from about 50° to 70°. Nearly 700 records of strokes were obtained, and the earth-wire/conductor angles of the lines investigated ranged from 22·5° to 70°.

In general, however, it seems doubtful whether the statistical data available at present are sufficiently consistent to prove or disprove any particular theory, and to accept as the minimum angle the smallest angle indicated by the statistics of any line is to leave open the question of the apparent immunity of other lines with greater earth-wire/conductor angles.

(6) CONCLUSION

Modern theories of lightning have advanced certain values for the parameters of the discharge. If these parameters are accepted, a theory of the shielding effect of an earth wire can be derived which indicates that no earth-wire position can be regarded as shielding phase conductors against all strokes, but that the fraction of strokes against which shielding is effective varies from zero with an earth wire in the same horizontal plane as the most exposed phase conductor to unity with the earth wire vertically above this conductor. If the earth wire is not required to intercept strokes which are not of danger to line insulation, the theory shows an angle of 45° between a vertical plane through the earth wire and a slanting plane through earth wire and phase conductor to be the limiting angle for adequate shielding.

(7) ACKNOWLEDGMENTS

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DISCUSSION BEFORE THE INSTITUTION, 4TH FEBRUARY, AND BEFORE THE NORTH-EASTERN CENTRE, AT NEWCASTLE UPON TYNE, 21ST MARCH, 1960

Dr. J. S. Forrest: Even in this country, where thunderstorms are not very frequent, lightning is still the greatest single cause of breakdowns of overhead-line systems, and anything which renders lines less susceptible will give a significant improvement in system reliability.

The author has made an important advance in the theoretical treatment of this problem, but the amount of practical progress is disappointing. To take one example, probably the most important conclusion in the paper is that a 45° semi-vertical angle should give adequate shielding, but this is, in fact, an empirical rule which we have used for years. It is given in the B.S. Code of Practice on protection of structures against lightning, which was first drafted 20 years ago. Even longer ago, I think that Benjamin Franklin had a shrewd idea that the cone of protection of a lightning rod was more or less of this form. It would have been more interesting if the author had stated that, contrary to the accepted view, 45° semi-vertical angle would not give adequate shielding!

A lightning flash is a very complicated phenomenon indeed. We must not imagine that a leader channel resembles a straight vertical line drawn on a sheet of paper and surrounded by a neat series of regular equipotential curves, and we should remind ourselves of what a lightning flash is really like. A photograph which I took shows, for example, a stroke to earth at an acute angle, and also a discharge which looks as though it might get under a line and strike it from below, though probably the current is too small to do much damage.

I will put in very simple terms what we regard as the main lightning problem to-day, namely the unexpectedly high fault incidence on very-high-voltage overhead lines. Fig. A shows the fault rates on two well-established systems, the 33 kV and the 132 kV Grid lines. These are accurate figures, based on statistics covering thousands of miles of line and extending over 30 years. The mean figures are 3·8 for the 33 kV lines and 1·0 for the 132 kV lines. With that information available, what is the fault rate going to be on a new 275 kV system, of the same general design as the 132 kV system? Lightning experts studied this problem in many countries and they all concluded that the fault rate would be very low, less than 0·5. In other words, they extrapolated the curve, as I have done, making the reasonable assumption that, as the insulation increased, the fault rate would fall. The actual fault rate for the 275 kV system, however, is about 1·6. This should be quite an accurate value, because we now have about 1 000 miles of 275 kV line in service and this figure is based on 3 600 route-mile-years of experience. Similar operating experience has been obtained in America.

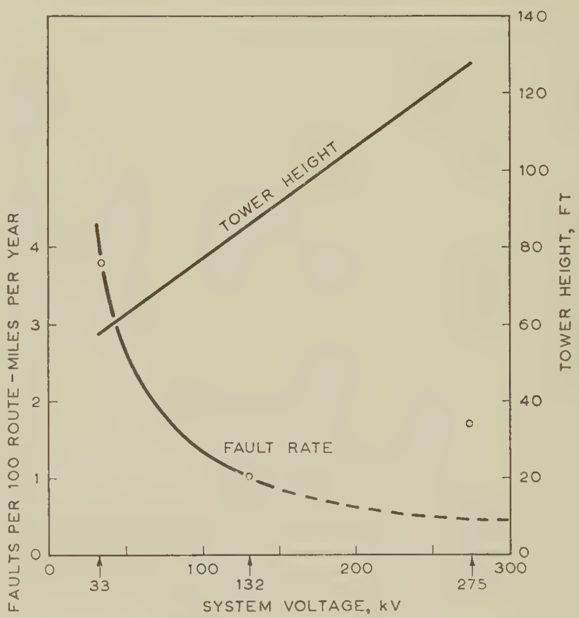

Fig. A.—Lightning fault rates.
Steel towers with single earth wire and low footing resistance.

It is true that the 275 kV towers are higher than the 132 kV ones, but it must be borne in mind that the 132 kV towers are higher than the 33 kV ones, and there is, in fact, no obvious discontinuity in the tower height which would account for the fault rate on the 275 kV system being four times the expected value. I should like to have the author's views on this problem.

Mr. W. Casson: Comparing the author's theory with operating

experience, I cannot agree with his conclusion that a shielding angle of 45° should be sufficient to protect the spans of conductors of an overhead line from direct lightning strokes. I have recently completed an analysis for C.I.G.R.É. of the operating experience of overhead lines throughout the world constructed for operation at voltages above 225 kV, since their installation, a report of which will be included in the Report of the Chairman of Study Committee No. 9 (Extra High Voltages) for the 1960 Conference.

This analysis shows that, with steel-tower lines, the lightning performance improves as the shielding angle is reduced, and with a 200-mile single-circuit line having two earth wires giving a shielding angle of 15°, and for an isoceraunic level of 15, there should only be one outage due to lightning every 7 years, which is practically perfect.

The analysis also shows that some lines have had no outages due to lightning since they were commissioned, one outstanding case being that of a line 430 km long which has been in commission for over 22 years; also, that lines without earth wires experienced lightning faults per year ranging between 0·25 and 0·6 per 100 km per storm day, depending upon the type of construction.

Auto-reclosing has been successful in reclosing circuits quickly after lightning faults, but even so, for high-security lines I consider it is operationally unsatisfactory to construct a line without an earth wire. An earth wire with shielding angle of 45° will reduce the faults to about one-quarter, and of 30°, to about one-eighth.

Further research is required on the shielding effect of an earth wire and I would like to have the author's views on the proposal to fit experimentally on an overhead line a shielding wire lightly insulated at each tower and open at the ends, in fact a floating shielding wire. Spark gaps to earth would be provided at each tower with devices to indicate flashovers. Surge recorders would be located at the ends and these would record all surges, including those that did not result in flashovers. Power-frequency voltage-to-earth measurements at the ends would enable the position of the flashover of gaps to be determined to some extent. This shielding method was described by Russian engineers some years ago and they claimed performance identical with a conventional earth wire.

Mr. D. F. Oakeshott: The author's criterion for calculating the shielding angle is a useful one, but for its practical application many assumptions have to be made. These include the form of $q(h)$ and the values of k, q_0 and h_0. Do the equipotential slopes vary rapidly with changes in these over a reasonable region? How critically do the bound-charge values control the generation of a leader process? Most important, what is the effect of a sloping leader? The one safe assumption is that it will never be vertical. If, statistically, the most common angle over the last and most influential portion of its path were, for example, 20° from the vertical, the equipotential surfaces would be tilted in their most influential region by a related amount, and the safe shielding angle would be correspondingly reduced because, on average, the tilt will only be favourable in approximately half of all cases.

Some data from America (12 000 mile-years) are reproduced in Fig. B. The points associated with the lower curve represent the fault rate on various 220 kV single-circuit lines, all of similar 'lightning-proof' construction. Variations in rate for a given angle occur, but it may be taken that the lowest rate is due solely to shielding failures. The curve drawn through the lowest points is very like that of Burgsdorf (Reference 16), also reproduced. Both curves have been applied to the British 275 kV system (shielding angle 45°) to calculate the shielding-failure fault rate: these come to 0·6 from the lower curve and 0·95 per 100 route miles per year from Burgsdorf's curve (using

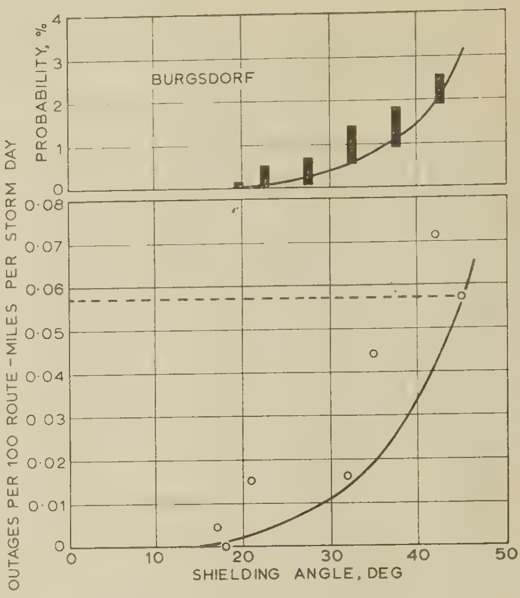

Fig. B.—Shielding angle data.

Reference 9). These figures are in fair agreement and are compatible with the latest one of 1·6 for the 275 kV system. Similar agreement was obtained in their application to an American 345 kV line* of construction similar to the 275 kV Grid but with 35° angle and ceraunic level 45. Rates of 1·3 (Burgsdorf) and 0·9 were obtained: special investigations on the line gave a figure of 1·5.

Since the interpretation of these curves seems reasonably consistent with experience, some reliance can be placed on their indication that, for adequate shielding, an angle nearer 20° is required.

Mr. G. B. Jackson: The greatest difficulty experienced by power engineers in presenting statistics of lightning performance on overhead transmission lines is in giving an accurate analysis of their operating experience. It is straightforward in most cases to blame lightning for an outage, but to get to the root of the problem it is necessary to subdivide lightning faults into their various forms in order to assess the efficiency of the earth wires. Modern high-speed protection coupled with rapid clearance makes fault location very difficult, and, with the demands of the system requiring the immediate restoration of circuits to service the analysis of a given fault and its location and the establishment of its cause are in many cases almost impossible. It is therefore necessary to resort to statistics.

I collaborated with Mr. Casson in examining the statistics from many tens of thousands of kilometre-years of experience and the most significant trend was obvious, namely the lower the screening angle the more effective an earth wire becomes. I do not think that it is possible to carry the deduction any further because no review of earth-wire performance is complete without taking into account the other important duty of an earth wire which is to screen the structure which supports it. I believe that the performance of an overhead line under lightning conditions is affected partially by the screening of the earth wire on the conductors in the span, and partly by the screening of the structure itself. The statistics which we gathered show a trend towards better protection on structures which, by accident or design, incorporate the necessary screening.

On 132 kV and 275 kV lines, and on lines capable of operating

* Schlomann, R. H., Price, W. G., Johnson, I. B., and Anderson, J. G.: '1956 Lightning Field Investigation on the OVEC 345-kV System', *Transactions of the American I.E.E.*, 1957, 76, Part III, p. 1447.

at 380 kV in this country, a 45° screening angle can be obtained on a double-circuit tower with the earth wire running through the peak of the tower, which is in fact, integral with the top cross-arm, so that the top of the structure is relatively unscreened. In many cases I have seen definite evidence of direct strokes to the structure.

Certain lines returned on the latest international survey showed extremely good lightning performances, in many cases with varying earth-wire screening angles, but with one common factor, that the configuration of the structure ensured its effective screening by the earth-wire. I hope that there will be an opportunity to follow up the effect of such screening, but it is rather a complex problem to solve in research as it is best done at full scale, which is not very practical.

I am sorry that the author did not make greater use of the operational statistics which are available and published in comparing his theory with his conclusions and with the conclusions which operating engineers have drawn from their own records. I am glad to see, however, that his limiting angle for earth wires coincides with the Board's designs. The evidence both in his paper and in our operating statistics shows that, to achieve improvement, earth-wire screening must be more effective, but, I would emphasize, it must be effective for the structure as well.

Dr. R. H. Golde: At a recent meeting of the Lightning and Surges Committee of C.I.G.R.É.* a full day was devoted to a discussion of the protective effect of earth wires. While the ultimate answer to this problem must rely on field experience, it was generally agreed that most useful contributions may be expected from an analytical treatment.

The author states that, even during the last stage of the leader process, the phase conductors remain at earth potential. Little is known about the mechanism of the last step of the leader stroke and the prevalent conception that the last leader does not differ from earlier steps may require revision. I refer in this connection to the recent work by Wagner† and his colleagues in the United States, from which it appears that the electromagnetic field which is created by the leader current and which precedes it with the velocity of light may materially affect the potentials of any conductors within its range.

A practical factor which must affect the shielding provided by earth wires is the wind. In assessing the shielding angle of a given line it seems insufficient to consider only static conditions. Positions should be considered when swinging earth wire and phase conductors are deflected in opposite directions.

I agree that the interpretation of the lightning performance of transmission lines is by no means easy. However, the uniform construction of the British Grid system offers a valuable source of information and I suggest that the author seeks access to data in the C.E.G.B. records and applies his critical faculties to their interpretation.

Mr. L. Csuros: Many aspects of lightning and electrical breakdown phenomena are problems of statistical probability. It may be an over-simplification to treat them on the assumption of conditions which occur in the majority of cases, ignoring completely the small statistical probability of unusual occurrences. Shielding against 99% of lightning strokes might result in ten times as many faults as shielding against 99·9% and, ignoring contingencies of small statistical probability, might render the treatment unsuitable for practical design purposes.

The basis of the author's treatment is the quite reasonable assumption that, if the most exposed phase conductor and the earth conductor are on an equipotential line of the electric field produced by the leader channel, then the chances are equal for the return stroke to be initiated either by the phase conductor or by the earth wire. He assumes that, if the earth wire is on a higher equipotential line, it will always send out an upward streamer to make contact with the leader channel and so shield the phase conductors. The implication that a very small change in the relative position of two electrodes placed in a high electric field would result in an abrupt complete change in the breakdown path is not supported by investigations on breakdown phenomena. Which electrode will be involved in the breakdown can only be determined in terms of statistical probability for relatively small changes in the positions of the electrodes.

The assumption in the paper that the geometrical configuration of the leader tip and the ground can be represented as a point-plane gap of 10–30 m is not justified in all cases, and the presence of trees, buildings or even of the overhead-line tower cannot be entirely ignored.

Another assumption is that the charge in the leader channel is invariably proportional to the peak current in the lightning stroke. Statistical data on lightning currents do not seem to support this.

Ignoring the perhaps small statistical probability of the conditions referred to here, the author's simplified assumptions seem to have led him to the conclusion that a shielding angle of 45° provides perfect shielding against all direct strokes with a peak current in excess of 2 kA. Operational experience with the e.h.v. Grid lines, which have a shielding angle of 45°, has, in fact, indicated that this is not the case. Nevertheless, I think the shielding angle adopted by the C.E.G.B. is the optimum figure for this country with the relatively low isoceraunic level obtaining. It represents the best compromise between the conflicting requirements of economics and perfection in lightning protection.

Mr. R. A. Hore: There seems to be very little doubt that the lightning statistics for the 275 kV lines came as a shock, and we know sufficient about back flashover to be sure that the major discrepancy between expectation and performance is due to shielding failures. It therefore seems that the author's figure of 45° is not correct. None the less, the general tenor of his results lines up well with the operating experience quoted by Burgsdorf and the model tests of McCann; the latter give optimistic estimates of the angle for perfect shielding. If, however, we adjust the model tests and the author's results, assuming that 20° gives perfect shielding, both then agree with Burgsdorf.

It seems to me, therefore, that there is a radical difference between model tests and the author's approach on the one hand, and actual practice on the other. I cannot see anything in the paper which would not apply perfectly validly to a model test, but this does not correspond with practice. Has the author any idea what it is that is so radically different, and does his theory apply to a model test?

I would mention a few of the many practical difficulties in comparing lightning statistics with theoretical conclusions. First, it is known how many line faults occur and, if there is a lightning storm at the time, the fault is generally attributed to lightning, but it is not so easy to find how many times the line is struck without suffering an outage. On some 330 kV lines we are installing a small gap connected between the lower leg and the counterpoise, and this gap will, we hope and expect, record the number of times that it flashes over and therefore the number of times that the line is struck. This should yield some fairly reliable data. Then there is the non-linear effect of tower height. It is clear that, since the leader stroke comes down to a certain height, if the tower height is not large compared with this, then few strokes will be attracted; but as soon as the tower height becomes large it attracts a lot of strokes. Also, towers are not necessarily built in straight lines on flat ground but may

* Provoost, P. G.: C.I.G.R.É. Report No. 314, 1960, Appendix II.

† Wagner, C. F., and Hileman, A. R.: 'A New Approach to Calculation of Lightning Performance of Transmission Lines—II', *Transactions of the American I.E.E.*, 1959, **78**, Part III B, p. 996.

be on the side or top of a hill or in a valley, and these factors are complicated by prevailing winds. All these things affect lightning statistics and we must be very careful about saying that certain statistics disprove a theory.

Mr. J. R. Smith: I can support Dr. Forrest from a particular case in Malaya, where a 33 kV line was struck in mid-span and actually burned down. The conductor concerned was the bottom phase, vertically below the phase above and also shielded by the earth wire.

Has the author considered allowing for the increase in the effective diameter of the conductors, particularly on higher-voltage lines, due to the formation of corona? Also, would the ionization of the air around the conductors due to corona affect the predicted results, particularly as some of the ionized air would be carried down-wind and might alter the conductivity of the air in the region of the conductors. Similarly, the corona itself would form elementary return leader strokes which would tend to make a return leader stroke form on the conductor rather than on the earth wire.

Dr. M. Ouyang (*communicated*): The author stated that the initiation of an upward streamer is determined primarily by the bound charge. It seems, therefore, that he considered the electric force due to the leader charge to be a secondary factor in determining the initiation of a streamer. However, in his calculations he used the curves suggested by Golde (References 9 and 12), although these are derived from the assumption that the initiation of a streamer is primarily determined by the electric force due to the leader charge. Will the author clarify how this apparent contradiction was resolved?

In the derivation of Fig. 6 from Fig. 4, the author tacitly assumed that a protective angle, say θ_1, calculated for a stroke current, I_1, provides no protection from strokes of smaller currents. This is surprising, because a stroke of no matter how small a current can hardly avoid an earth wire if it descends directly above it. Without going into calculations, with an angle θ_1 a stroke of current I_2 ($<I_1$) can still strike the earth wire provided that it descends at a horizontal distance from it not exceeding a certain value x. If x_{max} is the maximum distance at which this stroke will strike the wire in preference to the ground, then the angle θ_1 can protect against (x/x_{max}) 100% of strokes of currents I_2. Thus, Fig. 6 exaggerates the deficiency of shielding. Has the author considered this protection against strokes of smaller currents and found that quantitatively it can be neglected?

Granted that the assumptions and calculations are correct, Fig. 4 gives clear-cut relationships between the protective angle and the leader charge. However, as soon as the charge is converted into stroke current, an uncertainty is introduced because the conversion factor of 20 kA/C is only an average. No clear-cut relationship between the protective angle and the stroke current can be established.

Furthermore, imagine a stroke descending directly above the conductor and of a current so small that a streamer cannot start from the earth wire; this must strike the conductor unless the protective angle is zero. Therefore the curves in Fig. 4 must pass through the origin, perhaps asymptotically. I hope the above reasoning is faulty. If it is not and if Fig. 4 is extrapolated towards the origin, the consideration given in the last paragraph would indicate a considerable uncertainty in the limiting angle of 45° advocated by the author.

Dr. L. L. Alston (*at Newcastle upon Tyne*): Will the author indicate how the shielding effect of an earth wire predicted by his rigorous analysis differs from that predicted by Bewley's simpler method? I should be interested in the application of his analysis to a configuration with two earth wires.

The author has indicated that there are objections to experiments on models, so that his theory must be tested by experience. To assess the performance of an earth wire it would be necessary to install devices for counting strokes to the wire, as well as to the line conductors. It would still be necessary to differentiate between over-voltages produced by lightning and by other causes. Incidentally, strokes to conductors cannot be counted from the number of outages, because an over-voltage need not initiate power follow-current.

Mr. G. H. Hickling (*at Newcastle upon Tyne*): In connection with the effect of the diameters of the respective wires, probably nullified by the shielding effect of corona, it is of interest to note that under laboratory conditions discharges from surfaces of large diameter, although commencing at higher voltages than on fine wires or sharp-edged conductors, invariably occur with much more vigour once the critical surface breakdown stress is reached.

With regard to the 'attractive range' of overhead lines for lightning discharges, in view of the statement that the 'prospective voltage' is determined by the electrostatic field of the leader stroke, why is this range found to be dependent on the ultimate current in the main discharge?

In considering the minimum lightning current value against which shielding is necessary, it is obviously permissible, for strokes remote from the end of a line, to regard the surge current as dividing equally into two waves travelling in opposite directions; thus, for the minimum current of 2 kA quoted in Section 4, the surge voltage on a 500-ohm line would reach only 500 kV. Near to one end of a line, however, this division of current cannot take place, and the surge voltage would consequently reach twice this magnitude. It appears that this is a more important factor as regards the necessity for increased shielding near the end of a transmission line than that more usually quoted—namely the absence of the attenuating effect of damping in the line on the impulse wavefront.

Mr. A. B. Wood (*at Newcastle upon Tyne*): With overhead lines it is very difficult from service records to separate flashovers which are due to shielding failure from back flashovers, although in the protection of substations, gas storage tanks, etc., the observed data may be more reliable.

Statistical data indicate an angle of 20–30° for overhead lines, although a greater angle might be considered for substations, etc., because of the lower probability of a strike to such areas.

I agree that the step progression of the leader stroke has not been reproduced in model tests, although this might be possible in some gases at non-atmospheric pressure. However, model tests do enable comparative shielding results to be obtained, although the results are optimistic. With the model test data of Wagner, McCann and Lear,* but on the assumption that 20° gives perfect shielding for all practical purposes, a curve of shielding efficiency was derived which fitted almost exactly Burgsdorf's curve of observed data (Reference 16). This curve would explain the different outage rates of the four different designs of 275 kV line in this country. The protective angles of these lines vary from 20° in the best case to 45° in the worst. The curve of shielding efficiency also helped in designing the protection for the very large 330 kV switching station at Kariba. Here the isoceraunic level is probably over 100, and independent 200 ft masts were adopted to eliminate the possibility of back flashover.

The curves summarized in Fig. 4 depend on Dr. Golde's calculations of the leader tip heights for various charges. Might the assumptions made not be sufficiently far out to make a significant difference to the author's angle of 45°? Furthermore, to give 100% protection, should not the earth wire lie on an equipotential appreciably higher than that of the phase con-

* WAGNER, C. F., MCCANN, G. D., and LEAR, C. M.: 'Shielding of Substations' *Transactions of the American I.E.E.*, 1942, 61, p. 96.

ductor? I suggest that the 45° angle should be reduced by 10° or 20° to allow for the effects of ground side slope, slanted leader strokes, wind, differential conductor sagging, changes in protective angle at angle towers, sag difference due to temperature changes, varying ground clearance, etc. Incidentally, Burgsdorf concludes from his statistical analysis that it is unwise to exceed 30° and that 40–45° is quite ineffective. E.R.A. Report Ref. O/T14 also favours 30°, and Dr. Baatz of Germany told me that their results tend towards this same angle.

The effect of slanted leader strokes is often taken care of by placing the earth wire on the outside of the phase conductors on double-circuit construction. This is probably the explanation for the very satisfactory performance of single-circuit flat-formation lines with twin earth wires, the earth wires being widely separated and in many cases nearly vertically above the outer phase conductors. A useful publication* gives a detailed survey of the lightning performances of over 100 transmission lines in different parts of the world and shows the improvement in performance as the shielding angle is reduced. The angular definition commonly used can be misleading when the equipotentials are curves.

The author's method is a logical approach to a difficult subject, but I strongly suggest that a safety margin is necessary to cover the various unknowns.

THE AUTHOR'S REPLY TO THE ABOVE DISCUSSIONS

Dr. J. H. Gridley (*in reply*): A purely analytical approach to a practical issue can often be adjusted to produce a desired result. When the parameters involved form the basis of other established work, however, it is the assumptions behind the analysis which must be carefully examined if there is apparent difficulty in reconciling theory and practice. Dr. Forrest may have this in mind when he draws attention to the complication of a lightning flash, but such complication need not entirely discourage analysis. If the characteristics of a leader channel are even roughly those assumed, the field near earth is determined primarily by the lower few hundred metres of channel. Complication above this height is normally irrelevant and, if anything, would increase a shielding effect. This increased shielding would also occur if, as Dr. Golde suggests, phase conductors depart from earth potential, for they would then be nearer the ambient levels of potential than the earth wire and less likely to initiate streamers.

Mr. Oakeshott and Mr. Wood suggest slanting strokes as a possible cause of shielding failure and, as indicated in Section 4 of the paper, this has been considered. Calculation is extremely laborious and only the case of a 45° slant away from the line has been attempted. The required protective angle is found to range from 27° for a leader tip 5 m distant to 34° for one at 15 m distance. Certainly if the fraction of such slanted strokes is appreciable, the possibility of a significant number of shielding failures exists; Mr. Csuros correctly observes that the difference between 99 and 99·9% shielding efficacy is a factor of 10 in the shielding failure rate. His remarks on statistical scatter in streamer initiation are accepted; it is probable that Mr. Wood has this in mind when he suggests the phase conductor should be on an appreciably lower equipotential than the earth wire, but it is difficult to see how allowance could be made for this effect in analysis. Allowance for conductor swinging, as mentioned by Dr. Golde and Mr. Wood, and for changes in conductor disposition at angle towers, must, of course, be made to preserve any specified angle, but no direct allowance need be made for the corona formation mentioned by Mr. Smith since this is likely to affect phase conductors and earth wire equally.

As Mr. Wood observes, the development of the present theory depends on Dr. Golde's work on attractive range, but in Reference 10 some reasons are put forward for supposing the calculated curves of attractive range to have a significance not suspected when they were originally derived.

I agree with Mr. Jackson that shielding of the structure has not been considered, but I am not quite convinced that this should be regarded as a function of an earth wire. In reply to Dr. Alston's question concerning structures which carry two earth wires, I would apply the criterion of the paper to each earth wire individually.

In answer to Mr. Hickling, there are grounds for supposing return-stroke current to be a function of the charge on the leader channel, and Dr. Ouyang is correct in noting that the relation between shielding and stroke current is dependent on this correlation between current and leader charge. Dr. Ouyang's comment on Fig. 6 is also accepted, although the correction required is slight. On the other hand, there is no contradiction in the neglect of field due to the leader charge in comparison with that due to bound charge, despite the fact that the former charge induces the latter.

Approaches other than analytical have been suggested by Mr. Hore, Mr. Casson and Dr. Alston. For the validity of model tests I would refer Mr. Hore to Reference 8; experimental field installations as advocated by Mr. Casson and Dr. Alston have much more to commend them.

Dr. Alston's request for a comparison of the approach outlined in the present paper with Bewley's theory is not directly answerable, since the latter theory predicts no variation in protection with stroke intensity, but predicts a variation with cloud height which is not regarded as appreciable in the present argument. In general, Bewley's approach would give more optimistic results than the present analysis.

* 'Lightning Performance of Typical Transmission Lines', Ohio Brass Publication No. 1321–H.

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A NEW METHOD FOR OBSERVING THE PHENOMENA OF COMMUTATION

By H. J. H. SKETCH, B.Sc.(Eng.), Associate Member, P. A. SHAW, B.Sc.(Eng.), and R. J. K. SPLATT.

(*The paper was first received* 22*nd December*, 1958, *and in revised form* 25*th February*, 1959. *It was published in June*, 1959, *and was read before the* Scottish Electronics and Measurements Group *at* Glasgow 27*th October and at* Edinburgh 11*th November*, 1959, *a joint meeting of the* Measurement and Control Section, *the* Supply Section *and the* Utilization Section 5*th January*, *and the* North-Eastern Measurement and Electronics Group 1*st February*, 1960.)

SUMMARY

The paper describes a new method for measuring the armature coil current in a d.c. machine as it reverses during commutation. A small search coil, fixed relative to the armature, is magnetically coupled with one of the armature coils. The e.m.f. induced in the search coil, which is proportional to the rate of change of armature coil current, is integrated to give a signal proportional to the current itself, and this is displayed on the screen of a cathode-ray tube. The method overcomes several of the difficulties associated with the use of a shunt for measuring armature coil current during commutation.

A method is described for triggering the oscillograph time-base which permits examination of the current reversals as they occur in a number of consecutive revolutions of the machine.

The paper is illustrated with oscillograms showing the armature coil current during commutation in a particular aircraft generator, and the cause of poor commutation in this machine is deduced.

(1) INTRODUCTION

The design of d.c. generators for aircraft is difficult, and the occasional machine suffers from poor commutation. It is of some importance to discover the precise cause in each case, so that the trouble can be avoided, or overcome, in future designs. One of the most significant measurements made in such an investigation is of the current in an armature coil as it reverses during commutation.

Common methods of measurement use either some form of shunt in series with the armature coil,[1] or the coil itself as a shunt.[2] The potential drop across the shunt is applied to a pair of slip rings, which are temporarily mounted on the armature shaft, and the signal from the brushes is displayed on a cathode-ray oscillograph.

When any form of shunt is used, the e.m.f. induced in the loop formed by the shunt and potential leads must be kept small compared with the resistive drop across the shunt. In practice, this means that the potential leads must be most carefully positioned relative to the shunt. In the case of the particular aircraft generator examined, an attempt was made to use one armature coil as a shunt; with the best arrangement of potential leads possible, short of extensive modifications to the machine, inductive effects were by no means negligible, due in part to the high rate of change of armature coil current (approximately 1 MA/sec at full load assuming straight-line commutation). A special shunt connected in series with an armature coil presents a further difficulty. In order to preserve reasonable symmetry in the armature, so far as coil resistance is concerned, the shunt resistance, and hence the potential drop across it, must be small. With the machine used, the upper limit would have been about 1 mV on full load, and this was considered to be too low for the purpose of the measurements.

The paper describes a new technique for measuring armature coil current which overcomes these difficulties, and a method of synchronizing the oscillograph time-base which permits examination of the current reversals as they occur in consecutive revolutions of the machine.

The authors are at the Royal Aircraft Establishment.

(2) DESCRIPTION OF METHOD AND APPARATUS

(2.1) Current Measurement

A small search coil, fixed relative to the armature, was arranged to be magnetically coupled with one of the armature coils. The e.m.f. induced in the search coil, which is proportional to the rate of change of armature coil current, was applied to a pair of slip rings temporarily secured to the armature shaft; a simple integrating circuit, connected between the brushes, enabled the variation of armature coil current to be examined on an oscillograph. The use of some form of transformer and integrating circuit for current measurement is not new. For example, this method was used by Richter[3] to measure large alternating currents, and by Bennett and Dixon[4] to measure welding currents.

The experimental arrangement is shown in Fig. 1. External

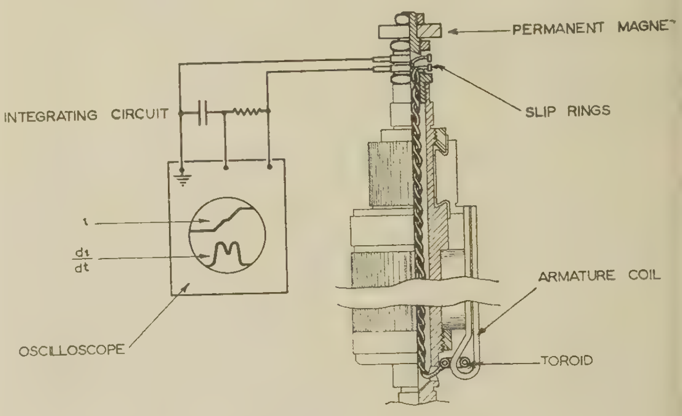


Fig. 1.—Measurement of current in armature coil.

apparatus and modifications to the armature are drawn with a heavy line. In the particular aircraft generator used, the only space available for the search coil was under the back overhang. The search coil consisted of a small air-cored toroidal winding, supported from the armature shaft by a resin-bonded fabric clamp. One armature coil was broken, and the connection was remade with a short length of copper wire linking the toroid. A search coil of toroidal form was chosen to ensure that the e.m.f. induced was predominantly due to the magnetic field associated with the selected armature coil. The armature shaft was hollow as far as the back overhang, and a small hole drilled in the shaft at this position enabled connecting leads to be taken to the commutator end of the armature, where an existing internal thread was used to mount a specially made extension shaft carrying the slip rings. These were made of stainless steel and the brushes were of silver graphite. The integrating circuit was simply a resistor and capacitor in series. The desired frequency response of the measuring system, when applied to the particular machine used, was estimated, and the various circuit constants were chosen accordingly.

With the toroid in position, but before the armature coil was

linked with it, measurements were made, throughout the load and speed range of the machine, of the stray e.m.f.'s induced in the measuring circuit. These proved to be approximately 1 mV compared with a signal of about 1 volt obtained on full load when the armature coil was subsequently linked with the toroid.

(2.2) Triggering the Oscillograph Time-Base

Fig. 2 shows the current in the instrumented armature coil as a function of the angular position of the armature; the various

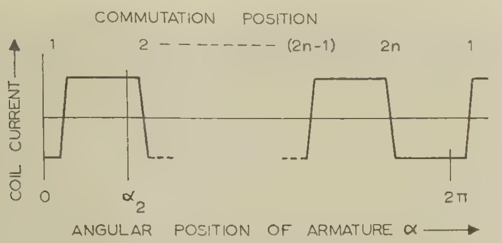


Fig. 2.—Commutation positions.

positions at which commutation occurs are numbered. In order to examine the current reversals at commutation position 2, for example, it is convenient to trigger the time-base when the armature reaches angular position α_2 and to use a sweep time that is a small multiple of the time taken for reversal of current. If the angular position of the armature at which the time-base triggers can be chosen at will, the current reversals associated with any commutation position may be examined.

Fig. 1 shows a 2-pole permanent magnet fitted to the machine, and it is redrawn in Fig. 3 together with the rest of the apparatus

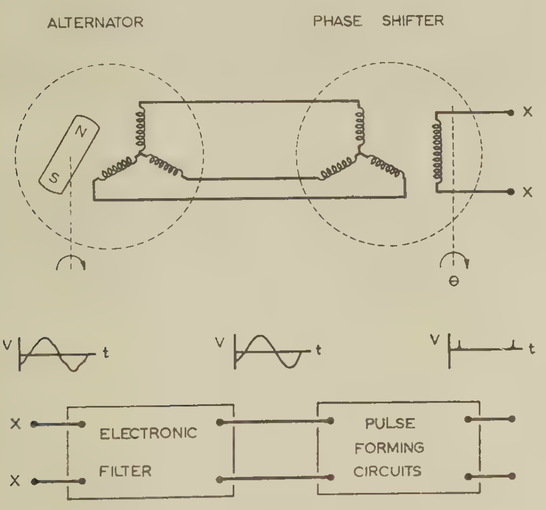


Fig. 3.—Apparatus for triggering oscillograph time-base.

used to trigger the oscillograph time-base. The permanent magnet was arranged to rotate in a stator carrying a 3-phase winding, and this winding was connected to a similar one in a synchro used as a phase-shifter. The e.m.f. induced in the single-phase rotor of the phase-shifter was filtered, squared and differentiated, and the resulting pulses were used to trigger the oscillograph time-base. With this arrangement any commutation position can be selected for examination by moving the rotor of the phase-shifter to the appropriate angle θ, and a suitable sweep time may be chosen with the normal oscillograph control.

With some modifications in technique, the current reversals can be recorded individually, on one frame of photographic film, as they occur in a number of consecutive revolutions of the d.c. machine. Two values of θ may be chosen so that the current reversals, associated with one particular commutation position, appear on the screen of the cathode-ray tube, either as shown in Fig. 4(*a*) (with $\theta = \theta_1$), or as in Fig. 4(*b*) (with $\theta = \theta_2$).

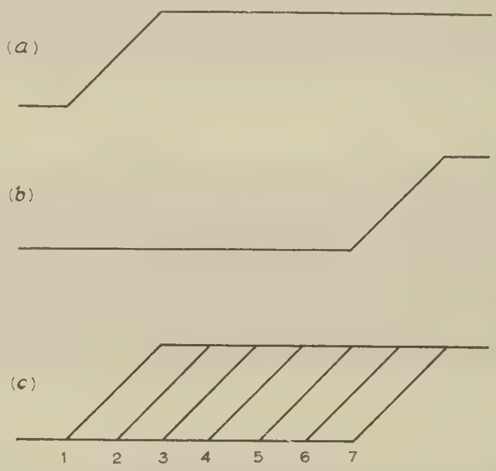


Fig. 4.—Commutation in successive revolutions of the machine.

If the rotor is moved rapidly from θ_1 to θ_2, with the time-base of the cathode-ray oscillograph adjusted to trip on each incoming pulse, the individual current traces will be separated on the screen as shown in Fig. 4(*c*), since the rotor will be at a different angular position as commutation occurs in each successive revolution of the d.c. machine. The rotor must be moved at an angular velocity sufficient to separate the individual traces, and they will be evenly spaced if the velocity is constant. When photographic records are made the camera shutter is opened, the rotor is moved from position θ_1 to θ_2 and the shutter is closed. Towards the beginning and end of this procedure, the rotor is stationary at positions θ_1 and θ_2 for a considerable number of d.c. machine revolutions while the camera shutter is open. As a result, in early experiments, the film was considerably over-exposed at positions 1 and 7 in Fig. 4(*c*). This difficulty may be overcome by using limit switches in conjunction with a lever secured to the shaft of the phase-shifter. With the lever at either end of its travel, a suitable potential is applied to one plate of the cathode-ray tube, which completely removes the traces from the screen.

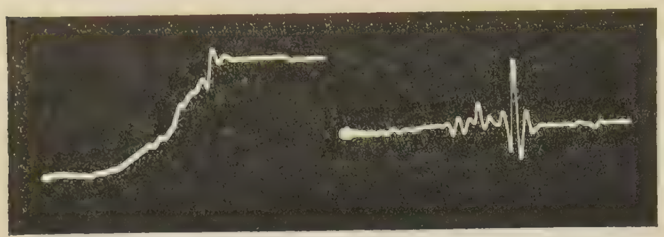
(3) DESCRIPTION OF MACHINE AND TEST RESULTS

(3.1) Description of Machine

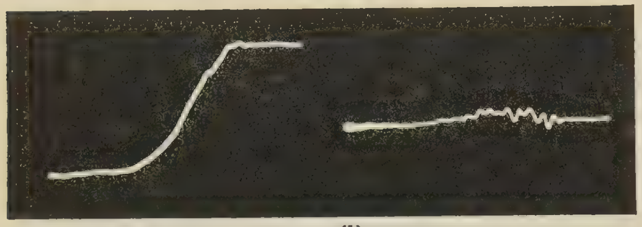
The machine used was a 6-pole d.c. aircraft generator rated at 112 volts, 200 amp; the speed range was 2850–10000 r.p.m., and the armature carried a retrogressive simplex wave winding in 85 slots. Commutation was known to be below the standard achieved in conventional aircraft generators of limited speed range. For test purposes the machine was run separately excited without a voltage regulator, and the existing auxiliary interpole winding was brought out so that the interpole m.m.f. could be adjusted.

(3.2) Results at Low Speed

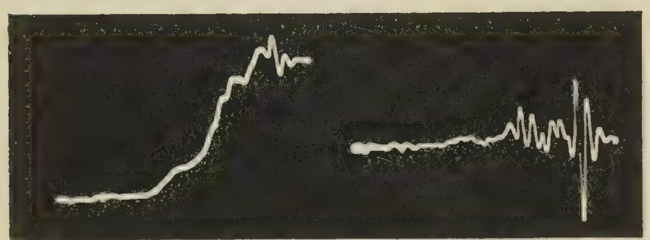
The machine was found to run at 5000 r.p.m. with a load of 100 amp without sparking, and the equivalent main interpole current could be varied by 3·75 amp on either side of the normal value before sparking was observed. Fig. 5 shows the result of a



(a)



(b)



(c)

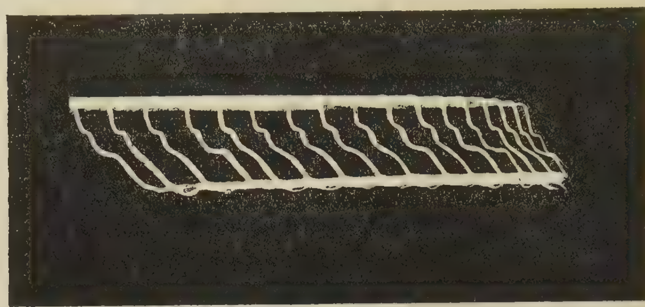
Fig. 5.—Oscillograms of armature coil current and rate of change of armature coil current.

Effect of large changes in interpole excitation.
Single-shot time-base;
Standard brushgear;
5 000 r.p.m., 108 volts, 100 amp;
Interpole adjustment (a) 28·5 amp buck;
(b) Best adjustment;
(c) 24 amp boost.

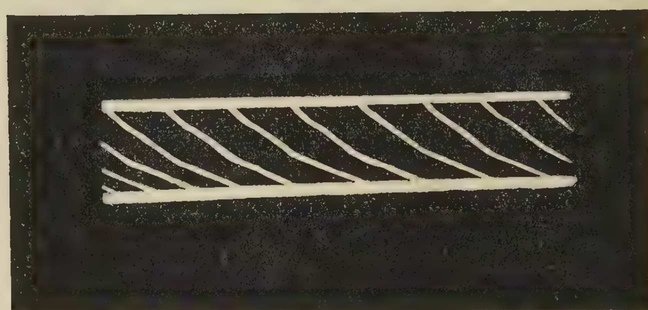
somewhat larger alteration in interpole excitation at the same speed and load; oscillograms of armature coil current and the rate of change of armature coil current are shown together. Fig. 5(*b*) was recorded with the interpole excitation adjusted for minimum step in current at the end of the commutation interval, this condition being judged from the shape of the corresponding oscillogram of rate of change of current. Fig. 5(*a*) shows the effect of bucking the equivalent main interpole current by 28·5 amp; commutation is retarded and the resulting current step is accompanied by a large spike on the oscillogram of rate of change of current. Fig. 5(*c*), which was taken with 24 amp boost, shows accelerated commutation with a reversal in the sign of the spike. Figs. 5(*a*) and (*c*) show irregularities in the current trace during the commutation interval, but Fig. 5(*b*) is comparatively smooth. This can be explained as follows. At any one instant in time there are several coils undergoing commutation which are at different stages in the process. If the interpole field is bucked or boosted, it is reasonable to suppose, from symmetry, that all armature coils have their current reversals retarded or accelerated in a similar manner. Thus, at some instants while the current is being reversed in the coil containing the measuring apparatus, other coils will complete commutation with a high rate of change of current, and this will affect the coil current measured.

(3.3) Results at High Speed with Standard Brushgear

When the machine was run at 10 000 r.p.m. with a load of 200 amp, severe sparking was observed at the commutator which could not be removed by adjusting the interpole excitation. This condition was accompanied by some instability in the shape of the coil-current oscillograms, and it seemed appropriate to use the technique outlined in the last paragraph of Section 2.2 so that the current reversals in a number of consecutive revolutions could be recorded on the same frame of film. Fig. 6(*a*) shows an oscillogram taken in this way at 10 000 r.p.m., 200 amp load, with severe sparking, while Fig. 6(*b*) was recorded at 5 000 r.p.m., 200 amp load, with no sparking. In Fig. 6(*a*) there are pronounced steps in the current reversals, and the shape of the trace changes from one revolution to the next; Fig. 6(*b*) is comparatively free from steps, and the shape of the current reversals is substantially the same throughout. Kluge[5] has shown experimentally that these steps are due to intermittent contact between the brushes and commutator. The differences in shape from revolution to revolution are attributed by the authors to the non-cyclic behaviour of the intermittent contact.



(a)



(b)

Fig. 6.—Commutation in successive revolutions of the machine with standard brushgear.

(a) 10 000 r.p.m., 112 volts, 200 amp. Severe sparking despite best interpole adjustment.
(b) 5 000 r.p.m., 112 volts, 200 amp. Best interpole adjustment and no sparking.

(3.4) Description of Special Brushgear and Results

In order to obtain better contact between brushes and commutator at high speed, a special set of brushgear was made for experimental purposes. The object was to increase the acceleration with which a brush is restored to the commutator after mechanical disturbance and to leave unchanged the dimensions of the brush face in contact with the commutator. Since the brush restoring force could not be significantly increased without overheating the commutator, efforts were made to reduce the effective mass of the brush and restoring device in motion. These considerations led to a considerable reduction in brush length,

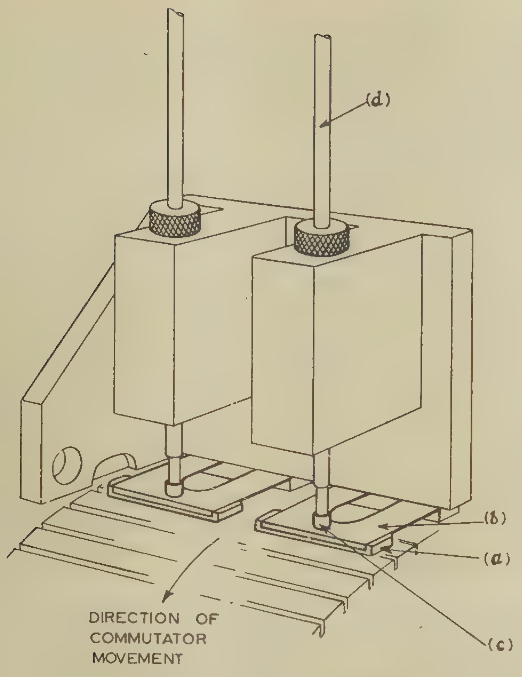


Fig. 7.—Special brushgear.

and the brush box, with its conventional restoring device, was discarded. The final design is shown in Fig. 7. A thin slice cut from a standard brush (*a*) was copper-plated on the back and soldered to an annealed phosphor-bronze foil (*b*), which, in turn, was secured to the brush arm. The foil prevented the brush from rotating with the commutator and served as the electrical connection. A small cup (*c*), silver-soldered to the foil, received a compressed-air supply pipe (*d*), and the normal brush-restoring force was obtained with an air pressure of 80 lb/in^2. In this way, the effective mass of the brush in motion was reduced to approximately one-tenth of the standard value. With this special brushgear commutation was found to be completely satisfactory throughout the load and speed ranges of the machine. Fig. 8 shows commutation in successive revolutions at 10 000 r.p.m., 200 amp load. There is very little change in shape from one revolution to the next, and the steps are much less pronounced than in Fig. 6(*a*).

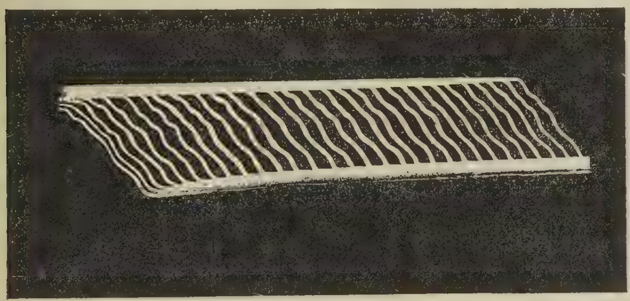


Fig. 8.—Commutation in successive revolutions of the machine with special brushgear.
10 000 r.p.m., 112 volts, 200 amp.
Best interpole adjustment and no sparking.

(4) CONCLUSIONS

A new method has been developed for measuring armature coil current during commutation which has the following advantages when compared with conventional methods:

(*a*) The difficulty of positioning the potential leads, associated with the shunt method, is avoided.
(*b*) A signal of reasonable voltage level can be obtained with negligible effect on the electrical symmetry of the armature.
(*c*) A simple test can be made, before the toroid is linked with the armature coil, to ensure that undesired signals are negligible.

A method has been devised for triggering the oscillograph time-base which permits examination of the current reversals as they occur in a number of consecutive revolutions of the machine.

(5) ACKNOWLEDGMENT

Acknowledgment is made to the Controller of H.M. Stationery Office, for permission to publish the paper.

(6) REFERENCES

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(4) Bennett, G. E., and Dixon, H. E.: 'The Measurement of Secondary Current, Voltage and Electrode Load Cycles for Spot Welding Machines', *Welding Research*, 1948, **2**, p. 26r.
(5) Kluge, H.: 'Die Kommutierungaszeit einer Ankerspule und die Stufenbildung in der Kommutierungskurve', *Elektrotechnische Zeitschrift*, A, 1955, **76**, p. 802.

DISCUSSION ON THE ABOVE PAPER

Before a Joint Meeting of the Measurement and Control, Supply *and* Utilization Sections *5th January*, 1960, *the* Scottish Electronics and Measurement Group *at* Glasgow *27th October*, 1959, *and the* North-Eastern Measurement and Electronics Group *at* Newcastle upon Tyne *1st February*, 1960.

Prof. A. Tustin (*at London*): The authors describe a method of recording coil currents which will be useful because it can be applied with little disturbance to the windings. They also give a few results of measurements, which are so interesting that one wishes they had given more. A comparison of Fig. 8 with Fig. 6(*a*) seems to provide a convincing demonstration that, on the machine in question, sparking was due to variation of contact between brush and commutator owing to rocking and bouncing of the brush or to irregularities in the semi-insulating skin that forms on the surface of the bars.

This is an important observation, and reinforces appreciation of the fact that two distinct mechanisms of surface deterioration may exist, which involve the inductance and mutual inductance of the coils in different ways. The first is the one usually considered in the theory of commutation, namely the existence of circulating currents due to uncompensated e.m.f.'s in the coils. The second is the possible forced transfer or switching of current from one bar to another due to variable contact. The current tends to be maintained by the inductance of the coils, and an interruption of contact with a particular bar will be likely to produce a short arc. It may well be that the considerable attention paid to obtaining correct electromagnetic compensation, assuming regular conditions of contact, must be supplemented by much more attention to the mechanical aspects of brush contact and of the conditions under which the film on the commutator surface remains sufficiently uniform.

Further information would be welcome about the oscillograph records reproduced in the paper. For example, Fig. 6(*a*), for the normal brush, shows clear evidence of marked over-compensation. The coil currents overshoot their final reversed value. Yet the authors state that the record corresponds to the 'best' interpole adjustment. This over-compensation is not visible in Fig. 6(*b*) and is not so marked in Fig. 8. Will the authors state what is meant by the 'best' interpole adjustment and whether it was the same in the various cases? It seems possible that they have more results of this kind than are given in the paper.

Mr. J. Rea (*at London*): D.C. generators for aircraft are invariably designed for the maximum possible ratings and, as a result, are frequently critical so far as commutation conditions are concerned. The machine discussed in the paper is a 112-volt d.c. machine, and the early stages of its production proved particularly difficult. In addition to the normal possible causes of poor commutation the picture was confused by a variable film of barium fluoride deposited on the commutator by the high-altitude brushes. The method outlined in the paper was one of the techniques by which it was established that the basic cause of the trouble was brush bounce at the maximum speed of the generator.

When the R.A.E. reported on the method used, a similar machine was constructed and very close agreement in the results was found. Some skill in interpretation is required, and if Fig. 8 is examined, it will be noted that there is still variation between successive traces, although there is a marked difference between these and the traces shown in Fig. 6(*a*).

During further investigation into different types of brushgear to reduce the amount of bounce, it was found desirable to use another method of examination in order to establish relatively slight differences between alternative constructions.

The arrangement shown in the paper with a wafer brush and air-pressure loading is very attractive from the point of view of low inertia, and it is unfortunate that a practical version does not seem possible. An acceptable standard for this generator has been reached by meticulous care in the construction of the commutator and by some reduction in the inertia of the spring and brush flexibles.

Prof. G. H. Rawcliffe (*at London*): I recall seeing at an exhibition a ring-type current transformer split into two halves, so that it could be clamped round a conductor. Similarly, the toroidal arrangement which the authors use might well be wound on two very small cores which could be clamped around the end-winding.

Did the authors consider using what is, in effect, a micro-current transformer? Their device might be considered to be a small current transformer, although it is a current transformer with an air core and not a magnetic core, which introduces a quadrature effect. It might be possible to vary this by having some kind of magnetic core in the toroid, made perhaps of the sort of material which is used for telephone loading coils. I feel that one could turn the authors' device into a very small current transformer, so that the output of the device would represent not the rate of change of current but the current, and an integrating circuit would be unnecessary.

It ought to be possible to inject a voltage into the interpoles which would minimize the undesirable effects at the commutator. Its frequency would have to be precisely related to the number of commutator segments, and it might perhaps be provided by a tiny inductor alternator. I am supported in this idea by the Figures in the paper which, even when they show bad sparking, also show regularity in badness. We ought to be able to have dynamic commutation; in other words, commutating-pole excitation which varies in step with any potential cause of sparking.

Mr. D. J. E. Evans (*at London*): We have learnt in the discussion of the academic interest and of the interest of the aircraft industry in d.c. machines. This, together with the recent paper by Mackay and Hardwick,* emphasizes the general interest of industry in d.c. machines, commutation, commutator wear and wear of carbon brushes.

I assume that a toroidal form of search coil was used, since the test machine had been manufactured before the present study was contemplated. Anyone in such a position will turn to the paper and use the method described. But, in the main, the use of such a coil enables only the current change to be studied. Are the authors considering the use of search coils similar to those mentioned by Prof. Tustin, which would permit the study of various internal voltages as well as current reversals during commutation?

If such a study were carried out, might it not be possible to correlate its results with those of the present work and with the results on the wear of carbon brushes similar to that described in a paper† by Sims? Such a study would materially contribute to a satisfactory answer to the problem of commutation and long brush life in d.c. machines. In this connection, the recent work of Dr. Haydn Ward‡ is important.

The authors state that stainless steel and silver graphite were used for the slip-rings and brushes, respectively. Were these materials chosen after others had proved unsuitable?

I note with approval that the brush holder developed for experimental purposes appears to be particularly sturdy. The success of the experimental brush gear is such that one wonders whether it has encouraged the authors to develop a practical design based on the principles.

Dr. H. Ward (*at London*): The most significant feature of Fig. 6(*a*) is that the current curves cross over at the bottom of that oscillogram. Since the bar current is equal to the difference between the currents in adjacent coils, neglecting equalizer currents, the bar current is reversing during commutation. Thus the direction of current transfer at the brush contact is non-uniform, which indicates very bad commutation. This would also be shown by oscillograms of bar-to-brush voltage.

My company has considerable experience in the measurement of armature coil current by using an armature coil as a shunt, with potential leads having an induced e.m.f. equal and opposite to that induced in the armature coil. This method is accurate where the potential lead is a 'shadow' foil adjacent to a strap-wound coil, but in two respects the new method of measurement is distinctly advantageous. First, with wire-wound armatures the shunt method is not practicable because one cannot readily devise leads which will have e.m.f.'s equal to those in the coil. Secondly, since the new method causes little disturbance to the winding, it may be acceptable for measurements on large machines on test.

Did the authors experiment with their new type of brushes using spiral springs instead of compressed air for maintaining the brush pressure?

The wearing depth of the authors' brushes appears to be less than $\frac{1}{10}$ in. This might give a brush life unacceptable for most purposes. Was the rate of brush wear measured? It seems that, with the better contact achieved by the new brush gear, wear may be greatly decreased.

Did the authors ascertain the minimum brush pressure required for satisfactory current collection at normal peripheral speeds, e.g. 5 000 ft/min?

* MACKAY, N. J., and HARDWICK, E.: 'Electrical Installation at Calder Hall Nuclear Power Station', *Proceedings I.E.E.*, Paper No. 2699 U, August, 1958 (106 A, p. 245).

† SIMS, R. F.: 'The Wear of Carbon Brushes at High Altitudes', *ibid.*, Paper No. 1505 U, July, 1953 (100, Part I, p. 183).

‡ WARD, H.: 'Commutation in D.C. Machines as Affected by Departures from the Ideal Flux Distribution', University of London Ph.D. Thesis.

Mr. J. G. W. West (*at London*): The measurement of current in an armature coil during commutation is an interesting research investigation. I was involved in the investigation of equalizer windings some time ago. A similar approach to that of the authors' would be useful for measuring equalizer currents.

I suggest that the rate of change of current waveform is a more sensitive measurement of bad commutation, i.e. sparking, than the current waveform.

My experience of the measurement of commutation is by measuring the r.f. sparking voltage at the brushes. A filter circuit blocks the direct voltage and slot-frequency and commutator-bar-frequency harmonics, and a valve voltmeter measures over the bandwidth 30 kc/s–20 Mc/s. Peak sparking voltages of over 4 volts have been measured. I consider this to be a satisfactory method of measuring commutation. It correlates to a certain extent with the intensity of sparking at the brushes and also with brush wear.

The mechanical causes of sparking and brush wear can best be studied by measuring the profile of the commutator and the movement of the brushes at the various operating speeds and temperatures. This can be done with a capacitance-type proximity meter.

Do the authors agree that the peak sparking voltage is a reasonable way of measuring sparking? Have they any experience of its correlation with commutator or brush wear?

Mr. W. I. Macfarlane (*at Glasgow*): The designer of generators for aircraft is required to take high powers from the smallest possible generator, often blast cooled and run at very high rotational speeds. He may therefore be unable to use as many slots and commutator bars as he would wish and the electrical design must generally be, at the best, a compromise.

In addition, the dry atmosphere at high altitudes will tend to wear away his brushes at a high rate unless they are made with special additives to improve the lubricating properties. The commutation process is thus made extremely difficult.

The obvious solution to the problems of brushes is to dispense with them, and the aircraft industry has done so successfully on alternators. Technology is now at a state when it seems possible that brushless d.c. machines may follow.

However, while 'old-fashioned' brushes and commutators remain it is necessary to ensure that the commutation field is set with a fairly high degree of precision to the best value. The authors' method of doing this has an elegance which is usually associated with all good experiments. It could be a useful tool for the test of normal commercial machines if the current-measuring unit could be simplified. Have the authors considered transforming the change of current in a coil directly on to the stationary member in any way? If this could be done the method might have considerably increased application.

I am also very interested in the special brushgear made up to eliminate inertia in the brush and its restoring system. Experience with commutators of dynamometers running at 5 000 r.p.m. has shown that intermittent contact, such as that indicated in Fig. 6(*a*), may be partially due to build-up of air pressure under the brush. The use of helical grooves cut in the commutator surface to reduce this pressure has in some but not all cases greatly improved commutation in spite of increased current densities under the brush.

Mr. F. C. Howlett (*at Newcastle upon Tyne*): The paper is on a method of observing commutation and not on commutation itself. As a method it is interesting, but its application requires such interference with the machine that its use must be limited to experimental machines.

I assume that the 85-slot armature had 85 commutator segments as it is a high-speed low-voltage machine. If so, this is a very simple winding and one would expect good commutation if the interpole strength is correct. Usually, however, there is more than one coil side in each layer of the slot, and it may be worth while applying the authors' method to individual coils of such a slot, as this may give an insight into the cause of blackening say every third segment which sometimes occurs.

The fact that the authors used 28·5 amp buck and 24 amp boost for Figs. 5(*a*) and (*c*), although the limit of good commutation was only $\pm 3 \cdot 75$ amp at the 100 amp load condition, may indicate that the method is not very sensitive. Would the authors say whether it is possible to detect the limit of good commutation by the oscillograph?

The most tangible result of the authors' investigation is that they tracked down the cause of the bad commutation at 10 000 r.p.m. The experimental brush gear eliminated brush bounce, as proved convincingly by Fig. 8.

The triggering of the oscillograph time-base is well thought out. Though simpler means could no doubt be found, the method gives a most convenient remote control.

Mr. G. H. Hickling (*at Newcastle upon Tyne*): It is evident from the paper that the air-cored toroid gives satisfactory sensitivity. Nevertheless, it would be interesting to know whether a dust-cored current transformer was considered as an alternative. Two 'C' cores each carrying half the winding, clamped round the machine conductor, would seem to offer a simpler arrangement, possibly involving less modification to the machine winding.

The authors deduce, by Fourier analysis of the ideal commutation current change, that a frequency response in the measuring circuit of up to 8 kc/s is required, corresponding to a harmonic amplitude not exceeding 1%. It is questionable whether this approach is valid, particularly for the rapid peaks in the rate of change of current as shown in the right-hand side of Fig. 5, and it would be interesting to know what is the frequency of the predominant oscillations shown in these traces. Could the authors also state whether any check was made of the effect of varying the upper response frequency of the circuit on the form of commutation current trace obtained?

It is perhaps worth recording that, although commutation difficulties on larger industrial d.c. machines are very exceptional, similar diagnoses have been made in one or two instances on alternator exciters, using the capacitance-probe technique to investigate commutator surface contour under full-load operating conditions. Fig. A is a record of the commutator surface during

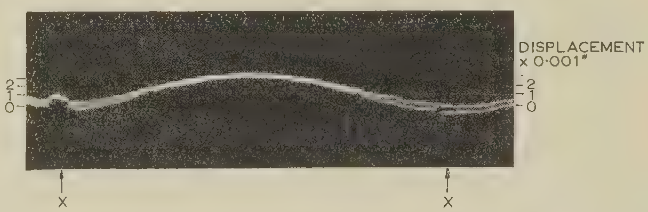

Fig. A.—Capacitance probe record of commutator surface contour.

Centre line of commutator (diameter approximately 1½ ft) at 3 000 r.p.m. on load. Note displacement of segments at X during one rotation of shaft.

a particular test, and indicates a defective section where about six bars were insecurely located, and were found to rise and fall during running. After improved fixing and regrinding at full running speed, satisfactory commutation was achieved.

With reference to Section 2.2, it is felt that the method described, employing two syncros, for triggering an oscillograph time-base involves an unnecessary amount of expensive equipment. Many modern oscillographs incorporate variable time-base delay, with which no additional equipment would

be necessary. Alternatively a very simple double-triode or 2-transistor circuit could be designed, which, on operation of a pushbutton (to charge a storage capacitor), would give a series of trigger pulses of progressively increasing time delay, thereby producing the effect shown in Figs. 6 and 8.

Mr. B. Berger (*at Newcastle upon Tyne*): If we consider a lap winding for simplicity, then, for linear commutation, the loop currents in successive coils will be displaced in time in a manner analogous to Fig. 4(*c*) (which refers to commutation in just one coil). Let us take two adjacent current reversals and imagine the area enclosed to be filled with a system of equally-spaced vertical lines. The commutator riser current will then vary according to the heights of these lines. For linear commutation the riser-current/time diagram is a trapezium of height I_r, say. For any other form of commutation the maximum riser current must be greater than I_r. Do the authors think, therefore, that the harmful effect of non-linear commutation is due to these periodic high brush current densities, and, if so, would they agree that the measurement of commutator riser current is more fundamental than coil current?

In the ingenious brushgear described in Fig. 7, I would like to know what grade of brush was used and the life of the thin brush wafer. Is the compressed-air feed flexibly connected to the cup? If not, as the brush wears, the pressure of the air spring must surely alter.

Mr. D. O. Burns (*communicated*): The authors themselves appear to deprecate any application of pneumatic brush gear to practical generators despite the success they had with the method experimentally. The disadvantages are presumably those purely of complication, it being presumed that air pumps and piping would have to be provided.

However, no flow of air is required if a leakproof device is used. This could be achieved by a light pre-pressurized air-encapsulating bellows acting on each brush segment. Hydraulically-formed bellows of very thin section (and therefore light) are available for this duty. Do the authors think this might be worth a practical trial?

Mr. F. P. Wills (*communicated*): The so-called 'black band' method of testing the effectiveness of the interpole is a simple way of determining the correct adjustment of compensation. It depends upon personal visual observation of the onset of sparking, which may, in some cases, lack precision.

In the brush voltage drop method of testing commutation, probes in the form of graphite pencils are inserted, contacting the surface of the commutator at regular intervals over the peripheral arc of the brush. The voltage drop is measured between each probe and the brush holder. When all the voltages are equal, the current under the brush is known to be uniform.

In the case of large machines with several brushes per arm the general current distribution is not much disturbed if one brush becomes a dummy with three to five graphite pencils mounted in an insulating block.

On aircraft generators, where there are usually not more than two brushes per arm, other methods must be used.

It has been found possible to drill one brush and insert graphite pencils wrapped around with a coil of one thickness of ordinary paper. With such insulated probes, even three, one at each of two diagonal corners and one in the axis of the brush, accurate measurements of voltages have been recorded.

The brush voltage drop method could form an additional check, and would show what is actually happening under the brush. This might be useful in the case of Figs. 6(*a*) and 8 since the pencils have very small mass.

Messrs. H. J. H. Sketch, P. A. Shaw and **R. J. K. Splatt** (*in reply*): We thank Prof. Tustin for his contribution and agree that more emphasis should be placed on the mechanical aspects of brush contact. With regard to the overshoot visible on most traces reproduced in Fig. 6(*a*), the coil current was unstable at the end of commutation when the machine was run under these conditions. The 'best' interpole adjustment was decided after examining current reversals corresponding to all six commutation positions, but with this degree of instability the adjustment cannot be precise.

In reply to Prof. Rawcliffe and Mr. Hickling, we believe that the design of a true current transformer for measurements of armature coil current might be difficult in view of the wide range of frequency involved. An air-cored toroid split in two could be used with some machines to avoid breaking an armature coil, but it would be impossible to measure the magnitude of stray induced e.m.f.'s with the toroid *in situ*, and this would be a considerable disadvantage. With regard to the suggestion of dynamic commutation correction, it is difficult to see that this would be the best solution in those cases where the brushes are mechanically unstable. All the current reversals illustrated in the paper occurred in about one thirty-fourth part of a revolution and thus the predominant oscillation in Fig. 5 is above 8 kc/s. However, Fourier analysis assuming straight-line commutation gives a useful guide to the frequency response desired in the measuring system when applied to any particular machine. A commutator profile indicator, using a capacitance probe and similar in some respects to that described by Mr. Hickling, was shown by Mr. R. H. Forrest at the Physical Society Exhibition in 1954. We used this device in the course of our work but no evidence of commutator instability was found. The use of variable time-delay in the oscillograph would still require a device which generated a pulse once per revolution of the machine under test with some precision in angle; this, together with the suggested double-triode circuit, would be about as complex as the arrangement we describe.

Mr. Evans mentions the slip rings and brushes used in the measuring circuit. The combination of stainless steel and silver graphite was adopted on the recommendation of colleagues who use strain gauges on rotating aircraft propellers, and no other materials were tried. Dr. Ward raises an interesting point concerning the relation between brush wear and mechanical stability. Under comparable conditions the rate of brush wear with the special brushgear was about one-fifth of that found with standard brushgear. The measurement of r.f. sparking voltage advocated by Mr. West is certainly more objective than a visual assessment of sparking in blackband tests, for example. Helical grooves cut in the commutator surface as described by Mr. Macfarlane were tried on our machine, but unfortunately they produced no improvement in this case. In reply to Messrs. Howlett and Berger no reliable indication of the onset of visible sparking was found in oscillograms of coil current and its rate of change. We prefer to examine the coil-current oscillogram itself rather than the corresponding riser-current oscillogram, which can be difficult to interpret. The main drawback of the special brushgear, so far as its use in aircraft is concerned, is that the brush length is insufficient to provide a reasonable safety margin during high-altitude flight. The relative merits of the pre-pressurized bellows mentioned by Mr. Burns and ordinary springs would have to be established by experiment.

With the generator mentioned in the paper each brush has to make about 14 000 satisfactory contacts per second with commutator segments at maximum speed. Even with commutators of the best construction available, the design of practical brushgear for use under these circumstances would merit further study. Measurements of brush motion relative to the commutator, possibly by means of miniature capacitance probes in the brushes, would be most useful.

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Aug. 1960

TRANSISTORIZED REGULATION AND CONTROL OF AIRCRAFT ELECTRICAL POWER SYSTEMS

By K. F. BACON, Graduate.

(*The paper was first received* 12*th November*, 1959, *and in revised form* 15*th March*, 1960.)

SUMMARY

The paper describes an attempt to replace with solid-state devices the carbon-pile regulators and electromechanical protective circuits in a representative aircraft's power system. A complete 2-generator d.c. system has been built and found to give substantial improvements in performance over a conventional system. The only electromechanical device used was a 400 amp d.c. contactor. A new approach to generator protection, relying on a knowledge of its excitation characteristics, is also described. There seems to be no reason why similar principles should not be applied to a.c. systems.

(1) INTRODUCTION

Aircraft power systems have in the past been dependent on such devices as carbon-pile regulators, sensitive relays and other potentially unreliable elements. The carbon-pile regulator has served the aircraft industry well for a number of years, but its stability, inefficiency and regulation limitations have been a constant source of trouble. Modern battery developments have led to more stringent requirements for busbar voltages, and these cannot be safely met with present regulator systems. The vibration and extremes of temperature encountered in modern aircraft have rendered inadvisable the use of sensitive relays performing reference and logic functions.

For these reasons, the use of solid-state devices for these purposes has increased. Magnetic-amplifier schemes alone have usually resulted in heavy and bulky equipments and so have not been very favourably received in aircraft, where additional weight is expensive. It should be noted, however, that at least one British aircraft used a magnetic-amplifier control scheme, as also did the German missiles in the 1939–45 War. The advent of transistors in recent years has lent a new lease of life to the magnetic amplifier, which, with its inherent gain stability, low zero drift and isolation, can be used for d.c. amplification and other functions with none of the disadvantages of size and weight. The large-output amplifiers can now be replaced with power semiconductors.

When the development of the present scheme was commenced it was realized that, although germanium transistors suffered from temperature limitations, silicon devices would be available in the near future. It was decided then to design a regulation, control and protection scheme for a 2-engine jet aircraft using germanium devices, so that the general principles could be proved and sufficient experience obtained so that conversion to silicon devices could be easily carried out at a later date. An aircraft with a 28·5-volt d.c. system was chosen for similar reasons, although it was realized that large aircraft in the future will probably use 3-phase systems.

(2) SPECIFICATION

(2.1) Generators

The generators are 9 kW 28·5-volt compensated shunt-wound machines driven via gearing from the main engines over a speed range of 2 850–10 000 r.p.m. The shunt field is wound for a maximum voltage of 26 volts to allow for a small voltage drop across the regulating device.

(2.2) Regulators

The generators must be regulated to an accuracy of better than $\pm 1\%$ over their full speed (2 850–10 000 r.p.m.) and load (0–300 amp) ranges. They must be capable of self-excitation and all circuits must be completely independent of external supplies, e.g. battery.

(2.3) Load Sharing

When two generators are connected in parallel, they must share the load current up to full load to an accuracy of 10% of the full-load current of one generator. When a generator is removed from the busbar for any reason, its load-sharing circuits must be disconnected. This prevents the other generator trying to take zero current through the action of the load-sharing circuits.

(2.4) Control

Each generator is connected to the aircraft's busbar via a main contactor and 300 amp h.r.c. fuse, the fuse being nearer to the busbar. This fuse protects the aircraft from large fault currents and hence reduces the risk of fire. Under normal conditions a 24-volt battery is connected to the busbar, but, as stated in Section 2.1, the operation of the circuits must not depend on this. The main contactor of each generator is to be closed when the generator voltage is equal to that of the busbar, or if the busbar is at zero voltage, when there is sufficient generator voltage to operate the contactor. The contactor is to be opened when a reverse current of 30 amp flows in the generator, and a warning light is energized in the pilot's cockpit. These functions permit the generators to be connected to and removed from the busbar automatically, depending on the engine speed.

The h.r.c. fuse could blow for either (*a*) an overload on the busbar, or (*b*) an earth fault between the fuse and the generator or in the generator itself. If it blows for condition (*a*), the generators are removed from the busbar and can no longer feed current into the fault, thus preventing any fire risk (the battery is also protected by a fuse). If the fuse blows for condition (*b*), however, the generator can continue to feed fault current, with subsequent risk of fire. It was therefore decided that, when the fuse blows for any reason, the main contactor should be opened and the generator de-energized by open-circuiting its shunt field.

(2.5) Protection

It is possible to derive an expression for the shunt-field current of the generator in terms of its speed and load currents, for a given constant armature voltage. A computer is used to solve this equation and compare the actual field current with the calculated value. If it differs by more than approximately $\pm 50\%$ for more than 1 sec, the generator is to be removed from the busbar and de-energized. The computer and control circuits

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.
Mr. Bacon is at the Mullard Research Laboratories.

associated with it are to be completely separate from the rest of the system, so that the generator is protected against internal faults and those occurring in the regulator. The computer must be as simple as possible, to minimize faults of its own components.

(3) REGULATOR

(3.1) Basic Principles

The schematic of the regulator is shown in Fig. 1. The output voltage of the generator is compared with a reference signal,

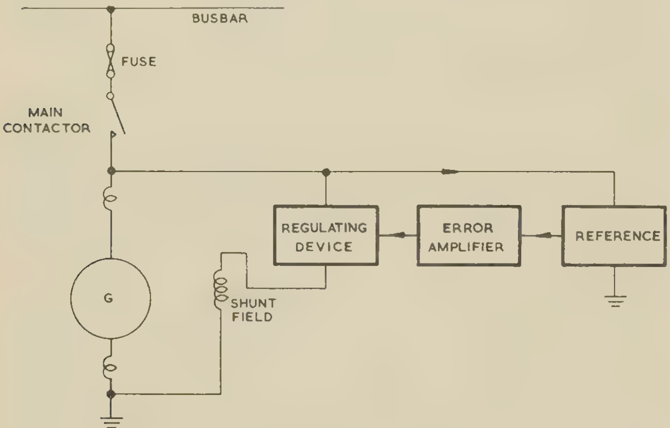

Fig. 1.—Block schematic of regulator.

and the error signal is amplified before being applied to the regulating device. This last stage alters the shunt-field current to reduce the error. The characteristics of the generator are given in Fig. 2, which shows the field current required for a

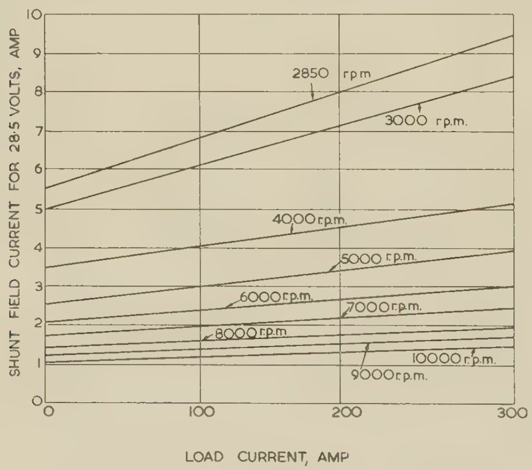

Fig. 2.—Generator excitation characteristics.

constant output voltage with varying load and speed. It can be seen that the field-current variation required is from 1 to 10 amp, giving a maximum dissipation of 260 watts for a field resistance of 2·6 ohms. Thus a series regulating device of the carbon-pile type must dissipate a maximum power of 78 watts, and is most inefficient.

The regulating device in the equipment to be described is a transistor,* which is unable to dissipate powers of this order, and so a technique similar to that used in the Tirrell regulator is employed. With this system the full field voltage is switched on and off rapidly at constant frequency, the 'on' period being variable. The average field current is then directly proportional to the duty cycle of the switching waveform. The basic circuit is shown in Fig. 3. The diode D_a acts to maintain the field

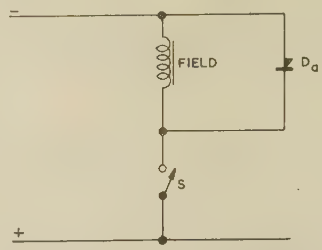

Fig. 3.—Regulator principle.

current during the 'off' periods of the switch and also to remove the large peak voltage occurring across the field at the instant the field is switched off. The inductance of the field smooths the current, and when steady-state conditions are reached the field current assumes a constant value. Thus if the frequency is f cycles per second, the 'on' period t seconds, the voltage V volts and the field resistance R ohms, the average field current is given by

$$I = \frac{V}{R} tf$$

It will be seen that, although the field current is practically smooth, the supply current is being switched on and off according to the duty cycle. This is because during the 'on' period the current flows via the switch S and the supply, and during the 'off' period no current flows from the supply and the field current circulates in the diode. This switched current will thus flow via the internal impedances of the supply (in this case the generator) and cause a voltage ripple on the output of the machine. To overcome this, the circuit shown in Fig. 4 was devised. Now

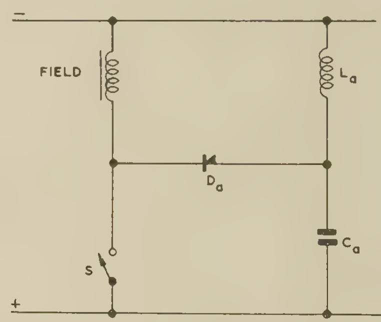

Fig. 4.—Filter circuit.

in the 'on' period, current flows from the supply via switch S and during the 'off' period, the cathode of the diode tries to go negative, allowing it to conduct and the capacitor C_a to discharge via the field and supply. C_a is chosen to have a low reactance compared with that of L_a at the switching frequency. Thus the current from the supply is no longer switched and the field current remains smooth. The only disadvantage of this circuit is that, because of L_a, the voltage across the switch is higher during the 'off' periods than in the circuit in Fig. 3, and the design of the circuit becomes a compromise between these requirements, since there is a severe collector-voltage limitation when S is a transistor.

(3.2) Switching Circuits

A circuit is required to generate a square wave of constant frequency whose duty cycle depends on the signal from the error

* British Patent No. 759566.

amplifier. A convenient method of doing this uses a magnetic amplifier of the self-saturating type connected in a full-wave circuit.[2] This method gives 1 kc/s pulses of variable length from a 500 c/s supply. A magnetic amplifier was chosen because some of the control applications (described later) could not be carried out without further magnetic amplifiers, and it became apparent that an a.c. supply to operate these would be necessary.

(3.2.1) 500 c/s Supply.

The 500 c/s supply was provided by a d.c./a.c. convertor of the bridge type,[3] the circuit of which is shown in Fig. 5. It was

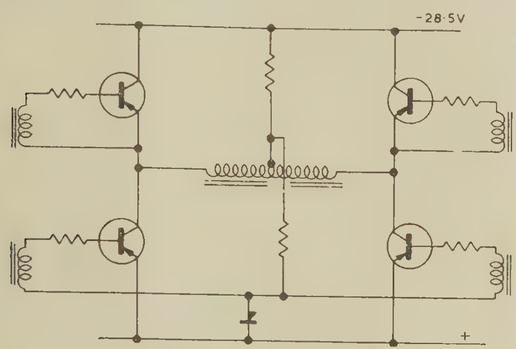

Fig. 5.—500 c/s oscillator.

designed to generate a 500 c/s square-wave with sufficient power to supply all the magnetic amplifiers used in the system; 500 c/s was chosen because it is approximately the most efficient frequency for magnetic devices of this sort from the aspect of losses and size; and because the variable-duty-cycle circuit would produce 1 kc/s pulses, so switching the field voltage at the same frequency. This is a reasonable compromise between low ripple and dissipation in the field-switching transistor. Since the output frequency and voltage of the oscillator are both directly proportional to the input voltage, it follows that the volt-second integral of each half-cycle is constant. This is ideal for magnetic-amplifier supplies, since these require a constant volt-second integral.

(3.2.2) Variable-Duty-Cycle and Output Circuits.

The square wave is supplied to the self-saturating magnetic amplifier responsible for generating the variable-duty-cycle waveform. Pulses of variable length appear across the load resistor (R_1 in Fig. 7) at a recurrence frequency of 1 kc/s. The magnetic amplifier uses toroidal cores of H.R.C. Alloy, the whole being potted in resin, and is approximately $1\frac{1}{4}$ in in diameter and $\frac{3}{4}$ in high. The characteristic of the amplifier with bias current to shift the characteristic to its mid-point is shown in Fig. 6, from which it can be seen that the duty cycle can be varied from approximately 5% (field current nearly zero) to 100% (field current maximum). The switching waveform is applied to the base of a transistor and thence to two other power transistors, which provide sufficient power to switch the field. The complete circuit is shown in Fig. 7.

Diodes D_1–D_4 provide a constant potential of about 2 volts which holds T_1 'on' in the absence of a signal from the magnetic amplifier. This voltage was necessary to prevent the voltage due to the magnetizing current of the magnetic amplifier turning off T_1. Positive bias for T_2 and T_3 is provided from the rectifier and filter D_5–D_8 and C_1 across a 12-volt winding on the oscillator transformer. R_2 is a 0·2-ohm resistor in series with the field, and is necessary because the field is wound for 26 instead of 28·5 volts. It is interesting at this stage to note that, because of the minimum resistance of a carbon-pile regulator, the generator

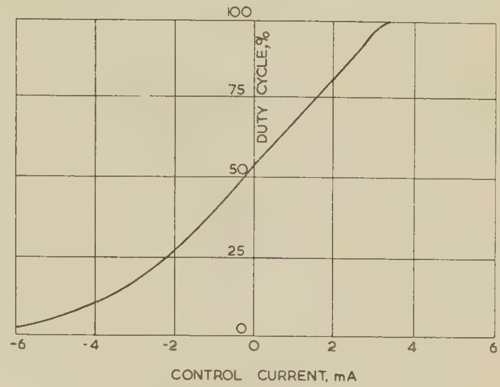

Fig. 6.—Magnetic-amplifier characteristics.

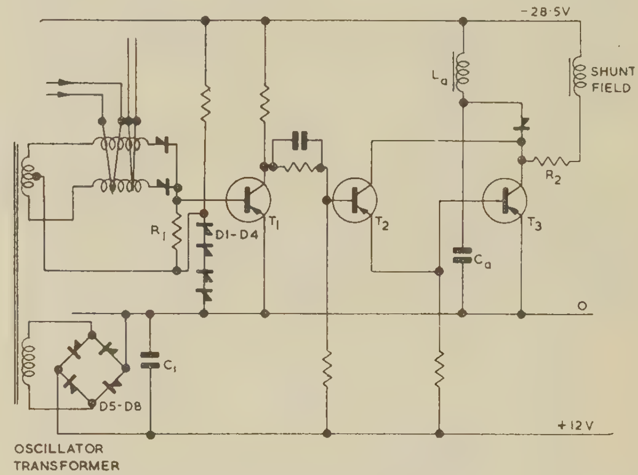

Fig. 7.—Regulator switching circuit.

had previously been wound with an 18-volt field. This resulted in a higher field current and larger losses in the regulator. Since there is a much lower minimum resistance with this new system, it has been possible to rewind the field for a higher voltage and lower current, with corresponding improvement in regulator efficiency.

(3.3) Automatic Excitation*

When the aircraft engines start up the residual voltage of the generator (0·5 volt) must be sufficient to ensure that the field transistor (T_3 in Fig. 7) is turned on fully to allow the field current to rise. Since the oscillator is inoperative at this voltage, and the characteristics of the magnetic amplifier are modified below about 15 volts, the circuit shown in Fig. 8 was designed to ensure automatic excitation. When the supply voltage is below 20 volts, Z_1—which is a 10-volt Zener diode—is non-conducting, so that T_4 is off and T_5 is on. This connects the base of T_2 (Fig. 7) to the zero line, so turning this transistor off and T_3 on. Thus the circuits to the left of T_2 (Fig. 7) are non-operative and the generator voltage rises. When this reaches 20 volts, Z_1 conducts, turning T_4 on and T_5 off and removing the base connection to T_2, so that the regulator circuit can function normally.

(3.4) Reference and Amplifier Circuits

The basic references for the regulator are Zener diodes. These can be obtained for a range of voltages from 3 to 100, the temperature coefficient of reference voltage being dependent

* Patent applied for.

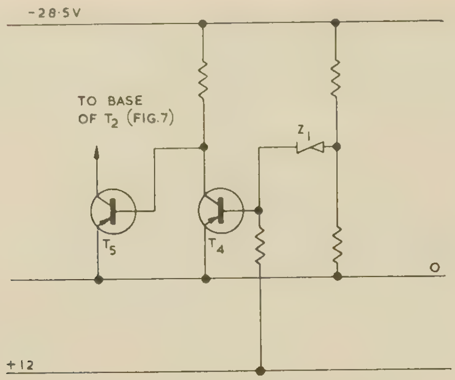

Fig. 8.—Automatic excitation circuit.

on this voltage; it is negative below about 5·2 volts and positive above this. The available Zener diode nearest to this voltage is a 5·1-volt version. Consequently the bridge shown in Fig. 9 consists of a number of these Zener diodes connected in

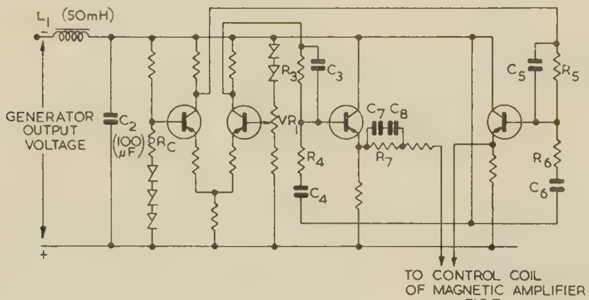

Fig. 9.—Regulator amplifier circuit.

each arm. In fact, slight compensation is still required, and this is provided by the copper resistor R_c. The variable resistor VR_1 is necessary to correct the 5% manufacturing tolerances of the Zener diodes. Since reliability is of utmost importance, this resistor consists, in practice, of a number of series connected fixed resistors.

The output of the bridge is taken to a long-tailed pair and from there into a pair of emitter-followers. These latter are necessary to provide high-impedance points for the insertion of servo correction networks. The circuit finally developed, including these networks, is shown in Fig. 9. The filter L_1C_2 is necessary to prevent overloading of the amplifier by tooth ripple. A further 200 μF capacitor is connected directly across the generator armature to help to reduce tooth ripple. Phase lag is provided by $R_3R_4C_4$ and $R_5R_6C_6$, and lead by R_3C_3 and R_5C_5 and $R_7C_7C_8$. To reduce the equipment size, tantalum electrolytic capacitors are used. Two capacitors, C_7 and C_8, are required, because these are also electrolytic and the voltage across them is bipolar.

(3.5) Frequency Response

To determine stability margins and the amount of correction required, frequency responses of the regulator were taken. Because of the large d.c. gain, complete open-loop responses were difficult to measure directly. The method adopted was to split the regulator into two units consisting of the switching and generator circuits and reference and amplifier circuits and to measure the responses of these circuits separately. The complete open-loop response was taken calculated from these two.

(3.5.1) Switching and Generator Circuits.

This response was measured by disconnecting the control and bias windings of the magnetic amplifier (Fig. 7) and connecting one to a variable-current source and the other to an a.c. signal source. The output voltage of the generator was set to 28·5 volts by means of the variable-current source and the alternating output voltage was monitored to measure the gain and phase of the system relative to the a.c. signal source. This was repeated for a number of generator speeds over its range and with varying loads. The effect of the battery was found to be a slight reduction in gain and increase in phase angle, and the effect of a load was a slight modification to the gain, since different loads require different field currents and the regulator operates over a different portion of its characteristic (Fig. 6).

(3.5.2) Reference and Amplifier Circuits.

The response of the reference and amplifier circuits was measured by varying the supply voltage with an a.c. source and monitoring the current flowing in the magnetic-amplifier control winding. To reduce the supply current necessary to do this, the circuits were disconnected from the generator and switching circuits and connected as the load of an emitter-follower. The amplifier gain and phase responses were first measured without correction. The amplifier had a nearly steady response up to 100 c/s, after which it fell steeply owing to the action of the filter L_1C_2 (Fig. 9). The overall response was corrected by reducing the gain of the amplifier at frequencies below 10 c/s and introducing phase lead at about 50 c/s.

(3.5.3) Complete Regulator.

When the responses of the above circuits are taken together, the total open-loop responses of the regulator are obtained. These are shown in Fig. 10 for various speeds and from them can

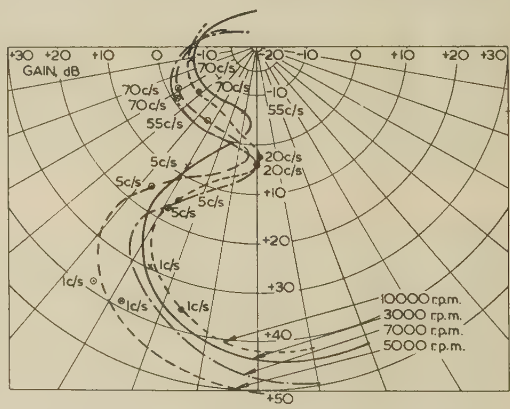

Fig. 10.—Nyquist diagram for regulator and generator.
No load; no battery.

be deduced the phase margins at cross-over. The values taken gave minimum phase margins of 60° without the battery and 50° with it. The effect of generator speed is to increase the gain of the system up to 6000 r.p.m., above which the increase in generator gain is offset by the decrease in gain of the magnetic amplifier by virtue of the non-linearity of its characteristic (Fig. 6). A series of frequency responses were measured for a second generator and regulator, and these showed that little difference in overall characteristics existed. It will be noted that the phase margin of 60° is adequate for all expected changes in production tolerances.

(3.6) Transient Response

When a load is connected to or removed from the busbar, the generator field current must change to a new value to keep the

output voltage constant. Owing to the time-constants of the system—due mainly to the shunt field inductance and resistance—this cannot occur instantaneously. The output voltage changes rapidly to a value outside the normal regulated tolerance and then returns to 28·5 volts as the regulator regains control. It is difficult to predict the performance of the system under these conditions, since the regulator amplifiers are saturated by these transients. A series of tests was therefore conducted to measure the amplitude and duration of these transients. The most dangerous transients for the transistor circuits occur when a load is switched off, since the transient voltage exceeds 28·5 volts by a considerable amount. Tests were therefore conducted with load switching from 80 to 20% of full load. A specification exists[6] which plots the permissible amplitude and duration of transients on an aircraft electrical system; this is reproduced in Fig. 11, and refers to load switching

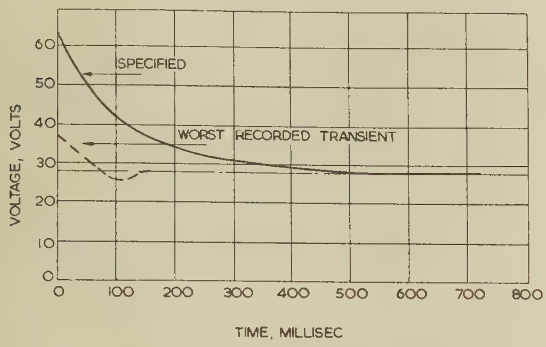

Fig. 11.—Transient response.

from 75 to 25% of full loads. Also shown on the graph is the worst transient recorded with the transistor regulator. The maximum voltage rise which occurred without the battery was 15 volts, and the maximum time for the transient to decay completely with the battery was 170 millisec, although at cruising speeds the time was 50 millisec. The effect of the battery was a reduction of the amplitude to a maximum of 7 volts and slight lengthening of the duration. To ensure that these transients have no ill-effect on the transistor circuits, transistors with a collector-emitter turnover voltage exceeding 60 volts are used. Exceptions are in circuits where there is no likelihood of the transients causing the transistor ratings to be exceeded.

(3.7) Regulation Accuracy

The total variation in output voltage with speed and load was found never to exceed $\pm 0{\cdot}25\%$, of which about $\pm 0{\cdot}18\%$ was due to speed variation, the remainder being due to load variation. Variation due to temperature was checked by subjecting the regulator to a temperature cycle, the generator remaining at room temperature. The output-voltage variation was found to be less than 1 mV/deg C, which represents less than $\pm 0{\cdot}16\%$ over a range from -40 to $+50°$ C. Allowing margins for production, it can thus be said that the overall regulator accuracy is $\pm 0{\cdot}5\%$.

(4) LOAD SHARING

(4.1) General Principles

When two generators are connected to a common busbar the voltage of the busbar will lie between the separate generator voltages. Each generator will supply a proportion of the load depending upon its output characteristics. This is illustrated in Fig. 12. As the load increases the generator voltage falls off or droops. Since the busbar voltage is constant at the level

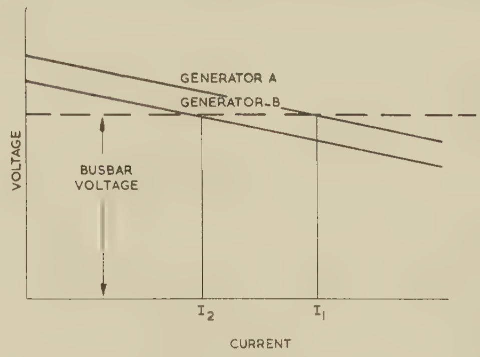

Fig. 12.—Load-sharing principles.

shown, generator A supplies a current I_1 and generator B a current I_2. Thus it can be seen that, for a given voltage difference, the bigger the droop the better will be the load sharing. Conversely, the better the regulation accuracy, the worse the load sharing. It is important that each generator supplies the same load current, otherwise, when one generator is loaded to full load, the other may be only partly loaded. Thus the full-load capacity of the system is limited to a value less than that of the two generators if they shared the load equally. Load-sharing can be achieved, as has been mentioned, by increasing the droop, but this results in loss of regulation accuracy. The method used for the transistor regulator is the same in principle as that used with carbon-pile regulators and can be understood by reference to Figs. 12 and 13.

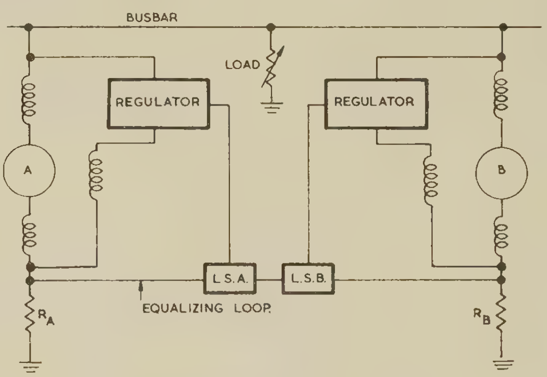

Fig. 13.—Load-sharing block schematic.

The current flowing in each generator is detected by means of the shunts R_A and R_B. If the current supplied by generator A is greater than that supplied by generator B, a current flows in the equalizing loop from R_A via LSA and LSB to R_B (LSA and LSB are load-sharing devices which act on each regulator). In this instance, LSA reduces the voltage of generator A and LSB increases that of generator B. Thus the characteristics of A and B (Fig. 12) move together and I_1 and I_2 tend to equality. This system will function perfectly, provided that there is always some inherent droop in the characteristics of the generators. Load sharing is impossible if the characteristics are perfectly flat, and the system becomes unstable if the characteristics rise, since the generator with the lowest terminal voltage supplies most current. Owing to the compensating windings of the generator, it is possible for the characteristic to rise slightly under certain conditions. However, since the generator is connected to the busbar via a length of lead which will add droop to the generator, and since this droop exceeds any rise which is possible, this condition is unlikely to occur in practice.

Thus it can be seen that a compromise between regulation accuracy and good load sharing is required. In some cases it might be necessary to build some droop into the regulation system so that load sharing became easier and to ensure that instability never occurred.

(4.2) Load-Sharing Circuits

The load-sharing devices LSA and LSB are magnetic amplifiers, and their outputs are arranged to influence the regulated output voltage in accordance with the equalizing signal. The circuit is shown in Fig. 14. The output from the magnetic

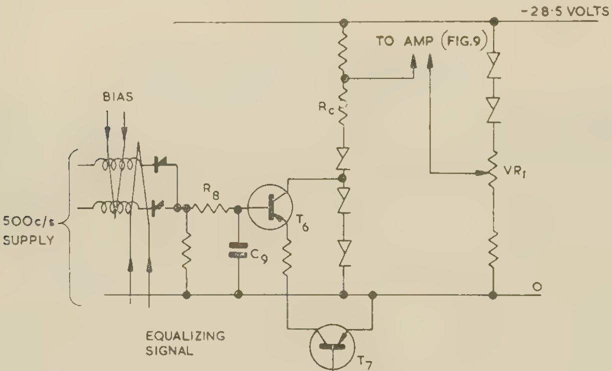

Fig. 14.—Load-sharing circuit.

amplifier is filtered by R_8C_9, and the d.c. output is used to vary the current flowing in the emitter-follower T_6 (T_7 is normally fully conducting). The current drawn via T_6 and the Zener bridge alters the stabilized voltage slightly and so varies the generator output voltage. The range of control is $\pm 1\%$ of the generator voltage, which is in excess of the difference between two generators. The function of T_7 is discussed in Section 4.3.

(4.3) Equalizing Loop

When a generator is removed from the busbar for any reason the load-sharing circuits for it must also be removed, since the load-sharing circuit of the other generators will act to reduce the current from its generator by lowering its voltage. It is therefore necessary to arrange that the equalizing loop is open-circuited under these conditions. Since current in the loop is bidirectional and electromechanical devices were considered inadvisable, a transistor bipolar switch was used. A magnetic amplifier was used to operate this switch because of the necessity for circuit isolation. The circuit used is shown in Fig. 15.

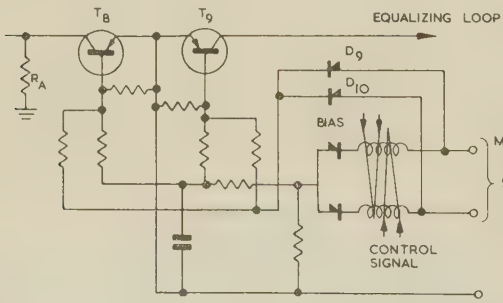

Fig. 15.—Equalizing-loop switch.

The output of the magnetic amplifier turns on T_8 and T_9 when it is in the conducting state. The base current is sufficient to bottom the transistors and current can then flow through them in both directions. When the magnetic amplifier is turned off by the control signal, the base current is reduced to zero and positive bias developed by D_9 and D_{10} from the 500 c/s supply holds the transistors off. This control signal appears whenever the generator is removed from the busbar. The same signal turns off T_7 (Fig. 14) and the regulated output voltage rises to its maximum value.

(4.4) System Tests

When two generators with their equalizing loop connected were parallelled to a busbar, it was found that they shared the load to within $\pm 5\%$ of the full-load current of one generator over the full load and speed variation. This is considered to be adequate for the system used.

(5) CONTROL CIRCUITS

The control circuits are responsible for the decisions for connection and removal of the generator to and from the busbar. The circuits also operate in conjunction with an h.r.c. fuse so that, in the event of an earth fault between the generator and the busbar, the generator is de-energized. The transducers for these functions are magnetic amplifiers because of the isolation necessary between the power and transistor circuits.

(5.1) Transducers

Each magnetic amplifier is connected in the circuit shown in Fig. 16. As the output of the magnetic amplifier appearing

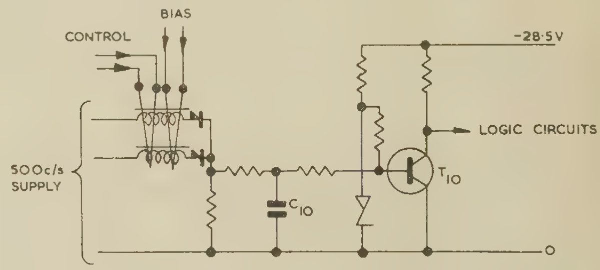

Fig. 16.—Basic switching circuit.

across C_{10} approaches the Zener voltage, the transistor is cut off. The output from T_{10} is taken to perform the necessary logical operation. The amount of control signal required to produce an output signal depends upon the bias setting and is different for the three functions.

Switch-On Transducer.—This magnetic amplifier senses the difference in voltage between the generator and the busbar, and has its control coil connected across the contacts of the main contactor. The bias current is such that, when this voltage difference is zero, an output appears from T_{10} (Fig. 16).

Switch-Off Transducer.—The control coil of this magnetic amplifier is connected across the shunt in the negative lead of the generator. The bias is such that an output appears when a reverse current of 30 amp flows into the generator from the busbar (e.g. when the engine is switched off).

H.R.C.-Fuse Transducer.—When the h.r.c. fuse blows a voltage will appear across its terminals. The control coil of this magnetic amplifier is connected between these terminals and an output appears when the voltage at this point exceeds 1 volt. Fig. 17 shows the respective connections of these three transducers for one generator.

(5.2) Logic Circuits

(5.2.1) General Principles.

It is apparent from the function required from the h.r.c. fuse circuit that, when the generator is de-energized, it must remain

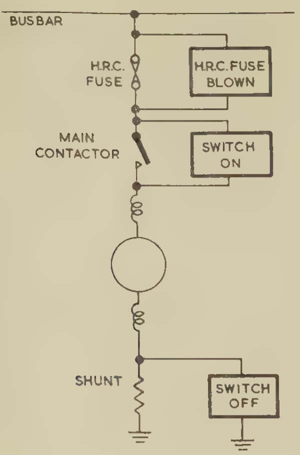

Fig. 17.—Switching-circuit block schematic.

so. This means that the supply voltage to the logic circuits falls to the remanent value of the generator, namely about 0·5–1 volt. Thus these circuits must be operable over a voltage range from about 0·3 to 28·5 volts. This automatically excludes conventional coupling between stages relying on a positive bias supply, since the oscillator generating this bias fails to operate at low voltages. It was therefore decided to use direct-coupled logic circuits for which no bias supply is required.[4, 5]

(5.2.2) Biased Flip-Flop.*

Since it is necessary for the signals from the transducers to operate a latched switching circuit, a flip-flop is necessary. However, when the generator starts up, a normal flip-flop would set itself into a state determined by the particular transistor parameters. Since these are indeterminate over a wide range for transistors of the same type, a biasing circuit is required to ensure that the flip-flop always sets itself into a predetermined state.

* Patent applied for.

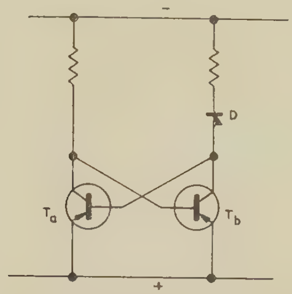

Fig. 18.—Biased flip-flop.

This is done by the use of a diode as shown in Fig. 18. As the supply voltage is increased from zero, the impedance of the diode D remains very high until about 0·3 volt. Below this voltage most current will flow into the base of T_b. Thus the circuit is biased so that T_b is on and T_a is off, and remains in this condition as the voltage rises. At voltages above about 0·7–0·8 volt the diode is fully conducting and the circuit functions normally.

(5.2.3) Complete System.

The circuit of the complete system is shown in Fig. 19. Flip-flops T_{11}, T_{12} and T_{15}, T_{16} are initially set by the collector diodes so that T_{12} and T_{16} are conducting. This ensures that the main contactor, MC, is off and the field circuit in the energized state. When the generator voltage equals the busbar voltage, MA2 operates and switches over T_{11}, T_{12} to energize MC. When a reverse current of 30 amp flows in the generator, MA_3 switches over T_{11}, T_{12} so that MC is de-energized. MA_3 also switches on T_{14}, which removes the signal from MA_2 to T_{11}, T_{12}. If the h.r.c. fuse blows, MA_4 switches off T_{13} to de-energize the main contactor via T_{11}, T_{12} and switches over T_{15}, T_{16} to open-circuit the field. This is done by the signal from T_{17} which open-circuits a further transistor in series with the emitter of T_1 (Fig. 7), turning off the field current. When

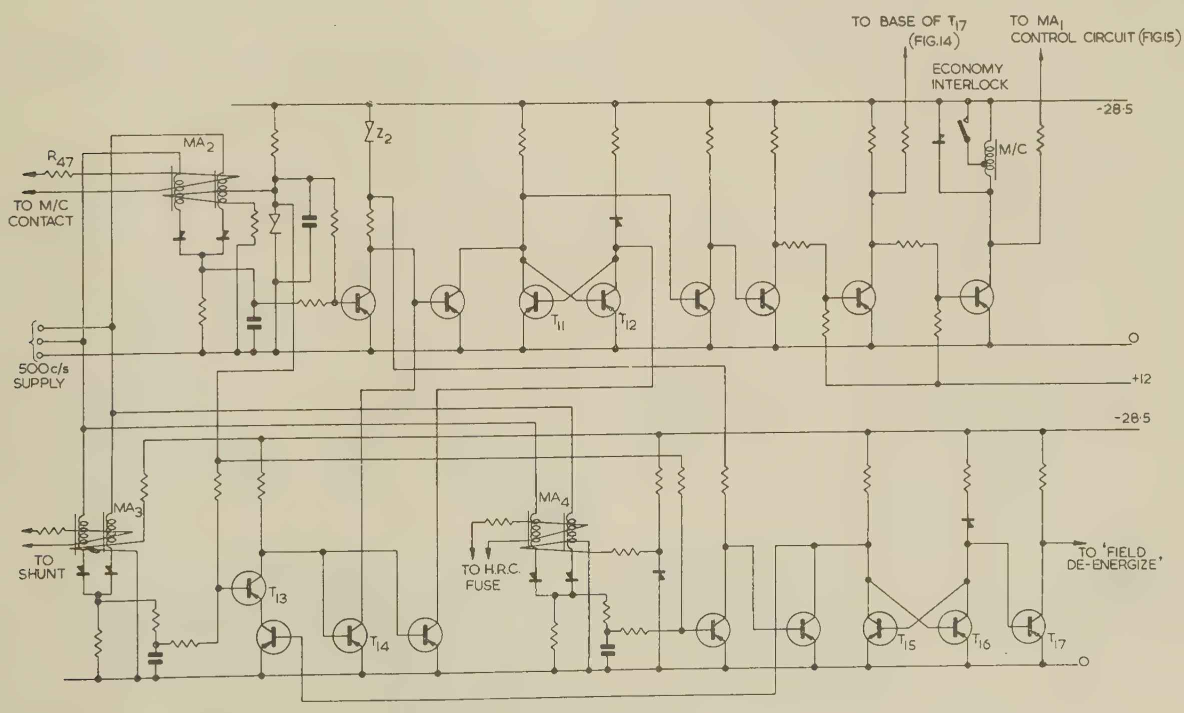

Fig. 19.—Switching and logic circuits.

this occurs the generator voltage falls to its remanent voltage, which at normal operating speeds is enough to ensure that the logic circuits retain the information that the fuse has blown.

Signals are taken from the main-contactor circuit to the base of T_7 (Fig. 14) and the control circuit of MA_1 (Fig. 15) to ensure that the load-sharing circuits are disconnected whenever the generator is removed from the busbar. Z_2 is included to prevent maloperation of the circuits when the supply voltage is below 10 volts. Should there be no battery connected to the busbar, its voltage will be zero, since there is always a small fixed load on the busbar, and the main contactor will be switched on as soon as there is sufficient supply voltage to operate it (about 16–18 volts).

(6) PROTECTION CIRCUITS

(6.1) General Principles

A number of protection facilities are desirable (e.g. over-voltage, under-voltage, earth-leakage, etc.), but the tendency in the past has been to limit these to absolutely essential facilities since the protective devices themselves add to the unreliability of the complete system. The scheme used in the present system is that of a composite protection circuit which will protect against all generator and regulator failures and yet remain as reliable as the rest of the system. This is done by a small computing circuit* which calculates the shunt field current of the generator required at given speeds and load currents, so that the output voltage is 28·5 volts. This is compared with the actual field current, and the generator is switched off if it differs by an appreciable amount. A time-lag is incorporated to guard against transient conditions. So that the protection circuits can guard against all regulator failures, including punch-through of the final switching transistor, the generator is switched off by separate circuits.

(6.2) Generator Characteristic Equation

The graph showing the field current required to give a constant output voltage of 28·5 volts at various speeds and load currents is shown in Fig. 2. The equation describing these characteristics is

$$I_F = \frac{10^5}{N^2} I_L + \frac{1 \cdot 61 \times 10^5}{N^{1 \cdot 3}} \quad . \quad . \quad . \quad . \quad (1)$$

where I_F = Field current, amp.
I_L = Load current, amp.
N = Generator speed, r.p.m.

A computer to calculate I_F from this equation would be rather complex, and a simplification was made as follows:

Since
$$\frac{a}{N^{1 \cdot 3}} \simeq \frac{b}{N^2} + c$$

the final term in eqn. (1) can be approximated into a function of N^2. Since the aircraft normally cruises at a generator speed of about 8 000 r.p.m., the approximation was made to fit at this speed. Eqn. (1) then becomes

$$I_F = \frac{10^5}{N^2} I_L + \frac{550}{N^2} + 0 \cdot 75 \quad . \quad . \quad . \quad . \quad (2)$$

Fig. 20 shows a plot of this equation compared with the actual graphs (Fig. 2).

It will be shown later that the errors at the low speeds can be reduced simply in the computer. Rearranging eqn. (2) gives

$$I_F N^2 = 10^5 I_L + 0 \cdot 75 N^2 + 550 \quad . \quad . \quad (3)$$

* Patent applied for.

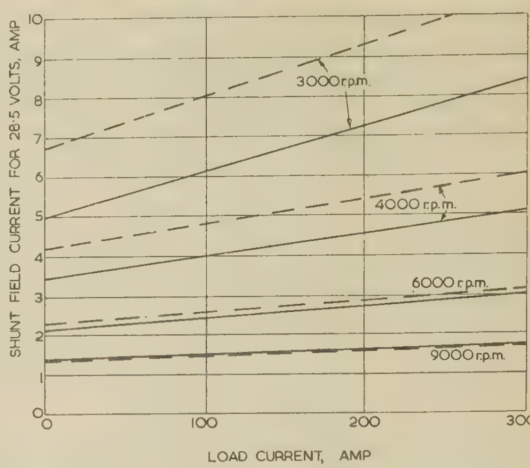

Fig. 20.—Generator and computer excitation characteristics.
—— Measured.
---- From eqn. (2).

The computing circuit thus multiplies the actual field current with N^2 and compares it with the right-hand side of eqn. (3). It will be shown in Section 6.5 that $I_F N^2$ does not vary very much over the complete range, so that the difference between the left- and right-hand sides is practically constant for a given percentage error in I_F. This simplifies the error detection.

(6.3) Block Schematic

The block schematic of the complete protection circuit is shown in Fig. 21. [The constants of eqn. (2) are redesignated K_1, K_2 and K_3 because of their conversion to electrical quantities.]

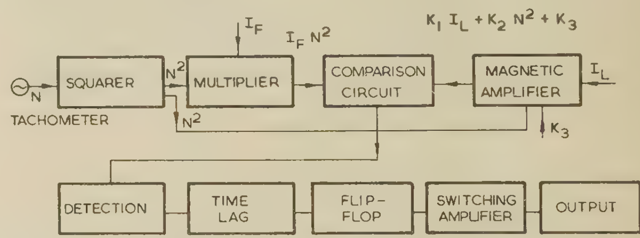

Fig. 21.—Computer block schematic.

A signal proportional to engine speed, N, is taken from the engine-speed-indicator tachometer and fed into the squarer. From this a signal proportional to N^2 is taken to the multiplier, where it is multiplied by the signal I_F obtained from the small resistance in series with the shunt field (R_2 in Fig. 7). The product $I_F N^2$ is then fed to the comparison circuit, where it is compared with the output from the magnetic amplifier. This amplifier multiplies the signal I_L, obtained from the load current shunt, by a constant K_1, and adds to it two signals $K_2 N^2$ and K_3. When the two quantities fed to the comparison circuit differ by a specified amount, the error-detection circuit operates, and if it operates for at least 1 sec, switches over the flip-flop which de-energizes the generator field via the switching amplifier and output stages.

(6.4) Squaring Circuit

The squaring circuit used relies on the fact that the voltage across an inductor supplied from a current source is proportional to the current and frequency of the supply. The tachometer is a permanent-magnet alternator and so lends itself admirably to this purpose, since the output voltage and frequency are both proportional to generator speed. Thus the voltage across an

inductor connected in series with a large resistor across the tachometer is directly proportional to the square of the engine speed. The output from the inductor is amplified via two transistors to provide sufficient power to operate the other computing circuits.

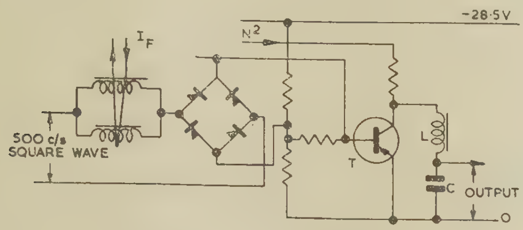

Fig. 22.—Multiplier circuit.

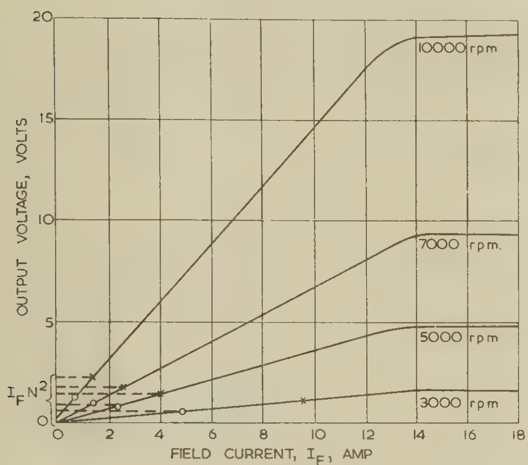

Fig. 23.—Multiplier characteristics.
× × Full load.
○ ○ No load.

(6.5) Multiplier

The multiplier circuit (Fig. 22) operates on the time-division-multiplex system. Consider a pulse waveform of constant repetition frequency: if the length of the pulse is made proportional to x and the height to y, the average height of the waveform is proportional to xy. The magnetic amplifier produces the pulse waveform of varying length in a manner similar to that of the magnetic amplifier in Fig. 7, except that, in this case, a simple saturable reactor is used to preserve linearity and stability. This pulse waveform switches off the transistor T whose collector supply voltage is proportional to N^2. Thus, since the pulse length is proportional to I_F, the output voltage after integration by LC is proportional to $I_F N^2$. The characteristics of the circuit are shown in Fig. 23 [$(10\,000\,\text{r.p.m.})^2 =$ 20 volts; 1 amp $I_F = 4$ mA control current]. The linearity is approximately 1%. Also shown on the characteristics are various values of $I_F N^2$ from which it can be seen that this value does not vary a great deal.

(6.6) Comparison Circuit

The comparison circuits consists of a long-tailed pair followed by a saturable-reactor type of magnetic amplifier. Feedback is applied to the long-tailed pair to render it insensitive to transistor-parameter changes. Since the magnetic amplifier and long-tailed pair are both equally responsive to positive and negative signals, the circuit is effective for increasing or decreasing field currents.

(6.7) Time-Lag Circuit

To render the protective circuit non-responsive to transient fault conditions, a circuit to ensure that the fault exists for approximately 1 sec is introduced between the error-detection circuit and the flip-flop. This consists of a CR network and Zener diodes as shown in Fig. 24.

When the output from MA_5 (Fig. 24) reaches the voltage of the Zener diode Z_3, T_{18} switches off and allows C_{11} to charge via R_9 and R_{10}. When the breakdown voltage of Z_4 is reached, T_{19} and T_{20} conduct, allowing a signal to be passed to the flip-

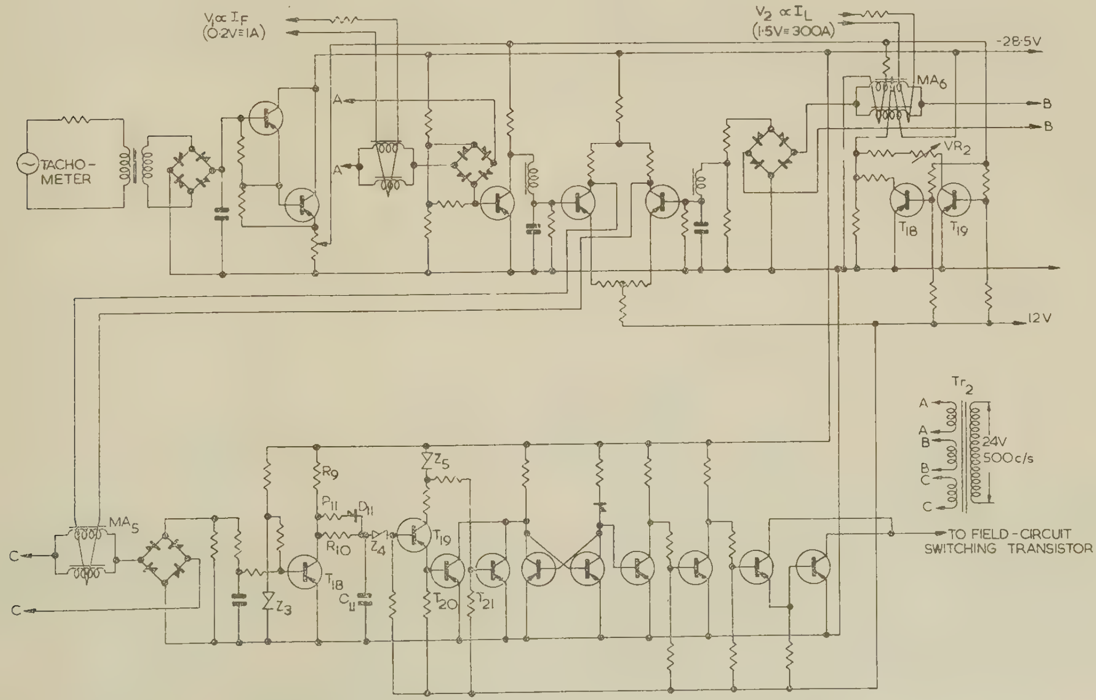

Fig. 24.—Computer protective circuit.

flop. D_{11} and R_{11} ensure that C_{11} discharges rapidly should T_{18} be turned off again by the clearing of a fault by other means. The purpose of Z_5 is to render T_{19} and T_{20} inoperative until the supply voltage reaches at least 10 volts. Should the 12-volt positive supply fail, T_{21} turns on, again switching the flip-flop.

(6.8) Complete Circuit

The complete circuit is shown in Fig. 24. Included in this are the means for correction of the computer characteristics at low-speed end of the range. Reference to Fig. 20 shows that the effect of the inaccuracy is a vertical shift of the characteristic, i.e. the addition of a factor to the right-hand side of eqn. (3). This factor varies with speed and approaches zero above about 5000 r.p.m. Thus this can be counteracted by a change in the bias of MA_6 below this speed, and this is done in two steps with T_{18} and T_{19}; T_{19} switches off as the speed is reduced below 5300 r.p.m., and T_{18} switches off when the speed reaches 3500 r.p.m. Thus the bias is changed in two steps which are sufficient for this purpose. It is difficult to predict the exact generator equation, especially at the higher speeds, since this depends on the generator brush position, which must be set for minimum sparking. The variable resistor VR_2 enables adjustments to be made which affect only the high-speed end and so balance out this effect.

Since, as was stated previously, $I_F N^2$ is practically constant, the control current at which the output from MA_5 is sufficient to switch T_{18} can be made a fixed quantity. This results in a spread in switching points which is most pronounced below 4000 r.p.m. Above this speed the switching point is about ± 35 to 40%, increasing slightly below 4000 r.p.m. This allows for considerable variations between generators and computers yet still provides sufficient protection from any catastrophic failure of components.

(7) CONCLUSIONS

A transistorized regulation control and protection scheme for a multi-generator aircraft power system has been described. The generators used have a maximum d.c. output of 9 kW at 30 volts, and there seems to be no reason why similar systems could not be designed for larger powers and for a.c. systems. The regulator has a considerably better performance and reliability than the carbon pile and is lighter than magnetic-amplifier circuits. The closer regulation accuracy will ease considerably the effect of temperature extremes on batteries, and could lead to easier design of other apparatus. The control circuits enable generators to be connected to the busbar at zero differential voltage (instead of about $+0\cdot5$ volt), so removing some transient problems. The protection circuit guards against failure of any of the above circuits and of the generator. There is no possibility of the removal of a faultless generator from the busbar, since over-voltage selection is unnecessary. Since all of the circuits are independent of external power supplies, the need for a battery to start the system is eliminated. This would be particularly useful in situations where ground supplies are unobtainable and the aircraft battery is discharged (e.g. after a forced landing). The advent of silicon devices will enable these circuits to be designed to operate at much higher temperatures.

(8) ACKNOWLEDGMENTS

The author wishes to acknowledge the very helpful advice and assistance received from Mr. M. Hancock and Dr. C. S. Hudson of the Ministry of Aviation and their staff. Thanks are also due to the authors' colleagues in the Mullard Research Laboratories, and to Mr. T. Reeve who carried out some of the experimental work.

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DISCUSSION ON 'APPLICATIONS OF ELECTRICITY IN AIRCRAFT'*

Mr. E. C. Railton (*communicated*): There appears to be a slight error in Section 2.3, where the author refers to Nypren cables. These were in fact introduced in 1953 and thus preceded Nyvin. The author may have had in mind the original Nyvin cable in 1957 and the lighter, smaller and higher-temperature-resistant Nyvin A cable in 1959. It seems, however, that the sentence should read: 'The introduction of nylon gave rise to the Nypren cables in 1953 and the Nyvin cables in 1957'.

I would also suggest that this paragraph should mention the use of silicone rubber in combination with glass and polyester braid in Tersil cables, introduced in 1957, the use of polytetrafluoroethylene in combination with glass braid in Glasef cables, introduced in 1951, and the use of a composite p.t.f.e./glass insulation in Efglas cables since 1957.

Mr. V. A. Higgs (*in reply*): I have to thank Mr. Railton for his comments and additional information.

* HIGGS, V. A.: Paper No. 3198, April, 1960 (see 107 A, p. 197).

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AN ELECTROSTATIC DUST MONITOR

By D. H. GRINDELL, Ph.D., B.Sc.(Eng.), Graduate.

(The paper was first received 20th May, and in revised form 8th October, 1959. It was published in January, 1960, and was read before the Supply Section *16th March, 1960.)*

SUMMARY

A smoke or dust particle carried by an air stream and subjected to high-voltage corona acquires a charge approximately proportional to its external surface area. If such ionized particles are precipitated at a collector electrode, the resultant flow of charge constitutes a current proportional to the rate of deposition of total dust surface, and a significant measure of the toxicity of a chimney discharge is obtained.

The paper outlines the theory of a dust monitor incorporating this principle, describes some experiments in smoke measurement and examines the operation of prototype equipment in a pulverized-fuel-fired boiler installation.

LIST OF SYMBOLS

M.K.S. units are used except where indicated in test.

V = Applied voltage.
V_s = Corona onset voltage.
E_s = Breakdown field strength.
E = Electric field strength.
E_p = Precipitating field strength.
R_0 = Radius of outer electrode.
r_0 = Radius of inner electrode.
i = Corona current per unit length of discharge wire.
K = Ion mobility.
$\lambda = 1/4\pi\epsilon = 8\cdot988 \times 10^9$ for $\epsilon = \epsilon_0$.
ϵ_0 = Absolute permittivity of free space $= 8\cdot854 \times 10^{-12}$.
ϵ = Absolute permittivity.
ϵ_{rp} = Relative permittivity of particle.
Q_0 = Limiting charge.
$P = 1 + 2\dfrac{\epsilon_{rp} - 1}{\epsilon_{rp} + 2}$
r_a = Particle radius.
r = Radius.
S = Total surface area of dust per unit volume of gas.
I_d = Dust current.
v_s = Gas velocity.
v = Particle drift velocity.
v_c = R.M.S. velocity of ions.
W = Volumetric gas flow.
$A = SW$ or dust surface per unit time.
τ_0 = Particle charging time-constant.
N_0 = Ion density.
e = Electron charge $= 16 \times 10^{-20}$ coulombs.
F = Force (mechanical).
μ = Gas viscosity.
n = Number of elementary charges.
k = Boltzmann's constant.
T = Absolute temperature.
M = Roughness factor.
$G = 3\cdot92b/(273 + \theta_g)$.
b = Barometric pressure, cm Hg.
θ_g = Gas temperature, deg C.
t = Time.
C = Constant.

(1) INTRODUCTION

In gravimetric methods of determining the quantity of solid matter carried in chimney gases, a measured sample of the polluted gas is filtered and the retained deposit is weighed; alternatively, the stain produced by the particles is compared with a standard. Similar apparatus is used to measure pollution of the atmosphere, and quantitative readings are expressed in either grammes per cubic metre or grains per cubic foot of dry sampled gas. In optical techniques, the obscuration of a beam of light projected through the smoke is measured and commercial instruments may be calibrated in terms of the Ringelmann smoke scale. This scale determines the degree of darkness of a smoke discharge by comparison with standard shade cards and has been chosen to define the limits of smoke emission prescribed in the Clean Air Act (1958).

However, particle surface and particle size seem to be more relevant than gross particle weight or mere smoke darkness to industrial fog formation, health hazards and the diffusion and transport of airborne materials. Thus the offensiveness of a chimney discharge would be better expressed in terms of the total surface area of solids entering the atmosphere. Furthermore, the most toxic particles are comparable in size with the wavelength of visible light, and so it becomes difficult to determine their concentration optically. Work was therefore begun to develop an instrument sensitive to particle surface and which, ideally, would provide a continuous record of the emission of smokes and dusts from chimneys. The basic principles of the electrostatic precipitator were adopted for this purpose.

A simple two-stage precipitator was constructed, as outlined in Fig. 1, with a particle charging section comprising an earthed cylinder and axial corona discharge wire, followed by a separate ion-free section with a radial precipitating field established between a central cylindrical electrode and an outer tubular collector connected through an amplifier to a microammeter. The discharge wire and the central collector electrode were supplied from separate high-negative-voltage sources. The prototypes, designed to provide a continuous or intermittent record of the ash carried in boiler-flue gases, and embodying an automatic self-cleaning action, were developed from this basic form and are now undergoing trials at a number of generating stations.

According to the theory of charging by ion bombardment, the number of electronic charges finally acquired by a spherical dust particle among gas ions in an electric field is proportional to the square of the particle radius. The mathematical treatment of this problem, although not complete, was greatly augmented by Pauthenier and Moreau-Hanot[1] in France some 30 years ago during their work on high-voltage generators having a dust-laden air stream as a charge carrier instead of the travelling belt used in the Van de Graaff machine.

There is also another process by which dust can collect gas

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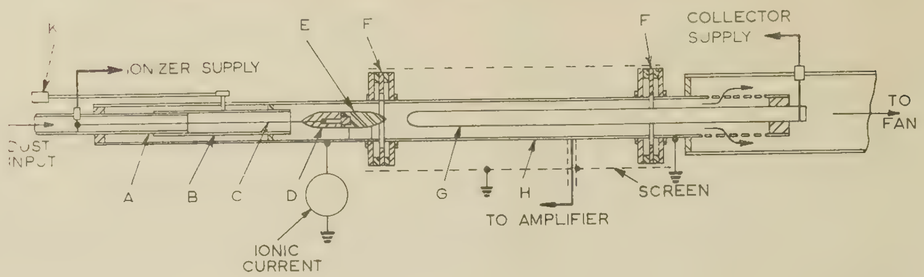

Fig. 1.—Simple smoke monitor in which the effective length of the discharge wire can be varied by means of the rod K.

A. Inlet pipe.
B. Sliding tube at the same potential as A and C.
C. Corona discharge wire.
D, F. Insulators.
E. Discharge-wire support and ion trap.
G. Collector central corona-free electrode.
H. Measuring electrode.
K. Control rod attached to tube B.

ions; this is known as ion diffusion charging and occurs, by present theory, without the assistance of an applied electric field. Collisions of ions with particles take place by virtue of the thermal gas motions, and the particle charge is then not strictly proportional to particle surface but follows a more complex law. It is important for smokes and finely divided dusts, and merits discussion here since the earlier experiments, some of which are to be described, were conducted with entrained cigarette smoke as a charge carrier. Dusts and grits from pulverized-fuel-fired-boilers, with which this work is mainly concerned, have a comparatively large mean particle size and ion-bombardment charging becomes predominant, although both processes must, to some extent, occur simultaneously. Figures relating to particle size in typical pulverized-fuel ash have been given by Tigges and Karlsson.[2] They state that only 0·0001% by weight, 0·164% by surface area and 16·15% by number of particles are less than 0·1 μ in diameter.

(2) THEORY

(2.1) The Corona Discharge and Particle Charging Mechanisms

In a tubular precipitator consisting of an earthed cylinder with a thin axial corona wire maintained at a high negative potential the field intensity is high at the inner electrode and falls to a low value near the outer electrode surface. A corona discharge forms around the inner wire, preceding flashover, when the electric field there exceeds the critical value if the ratio between the outer to inner electrode diameters is, in theory, greater than 2·72. Experimentally, this ratio is somewhat dependent upon the diameter of the outer electrode and a value of 10 has been found.[4]

The field intensity at which corona first appears may be obtained from an empirical expression derived by Peek,[3] namely

$$E_s = 3{\cdot}1GM\left[1 + \frac{0{\cdot}0308}{\sqrt{(Gr_0)}}\right] \text{ megavolts per metre} \quad (1)$$

where G is a factor which takes account of variations in barometric pressure and the gas temperature; M allows for the roughness of the corona wire and is unity if the wire is smooth, falling to about 0·8 if it is rough or dirty.

The voltage at which corona occurs may be calculated from the classical equations for fields of this type:

$$V_s = E_s r_0 \log_\varepsilon \frac{R_0}{r_0} \quad . \; . \; . \; . \; . \; . \quad (2)$$

In the corona discharge, electrons in the most intense part of the field near the central wire possess sufficient energy to produce positive ions and other free electrons by collision with gas molecules. Excited atoms may produce photo-ionization in the gas and photo-electrons may be emitted from the electrode surfaces. The process is cumulative and an 'electron avalanche' proceeds towards the outer electrode; because of their lower mobility, the positive ions are left behind to create a positive cloud. This, together with the fact that the field decreases in the radial direction, has the effect of restraining the progress of the avalanche, and no spark occurs. The electrons, moving more slowly as they approach the outer cylinder, become attached to gas molecules, so forming negative ions, and these in turn drift towards the positive electrode. The negative ions constitute a second cloud, which extends throughout the inter-electrode space except for the small volume in the immediate vicinity of the inner wire. They strike any dust particles present while following the lines of force of the distorted field around each particle, or merely collide with the dust during their random Brownian movements (ion-diffusion charging). With negative corona, a high proportion of the dust is charged negatively, but some, close to the discharge wire, is positively charged and may be attracted to it.

The ion current per unit length of discharge wire has been determined by Townsend, who obtained the following equation, valid for low currents at $V > V_s$:

$$i = \text{constant} \times V(V - V_s)$$

The constant depends upon the electrode arrangement and the properties of the gas.

Thus, for concentric cylinders,

$$i = \frac{2K}{\lambda R_0^2 \log_\varepsilon \dfrac{R_0}{r_0}} V(V - V_s) \text{ amperes per metre} \quad . \quad (3)$$

(2.2) Particle Charging by Ion Bombardment

Pauthenier[1] has shown that dust particles in an ionized electric field quickly acquire a limiting charge depending upon their radius, dielectric properties and the field strength, and he derived the following expression:

$$Q_0 = \frac{PEr_a^2}{\lambda} \text{ coulombs} \quad . \; . \; . \; . \; . \quad (4)$$

where $P = 3$ for a conducting particle.

The particles are assumed to be spherical and uniformly distributed. It is understood that only negative ions are present, that their mobility is constant everywhere and that i is uniform along the length of the corona wire.

For an ion to approach a dust particle, the resultant of the forces acting between the two must be attractive. These forces are due to (*a*) the charge already on the particle, (*b*) the distorted field around the particle in which the ion is situated, and (*c*) the electric image of the ion in the particle.

(2.3) Field Strength in a Tubular Precipitator

For low ion and dust concentrations in the inter-electrode space the field at any radius r is determined by the classical expression

$$E = \frac{V}{r \log_\varepsilon \dfrac{R_0}{r_0}} \text{ volt per metre} \quad . \quad . \quad . \quad . \quad (5)$$

If ionization along the length of the central electrode is not negligible, this field is modified by space charge and may be determined from the equation

$$E = \sqrt{\left(\frac{2\lambda i}{K} + \frac{C^2}{r^2}\right)} \quad . \quad . \quad . \quad . \quad . \quad . \quad (6)$$

where C is a constant.

If i is large and r is not small, this approximates to $E = \sqrt{(2\lambda i/K)}$—a value which underestimates the field everywhere except near the outer electrode surface. Nevertheless, it provides a useful estimate in the design of large industrial precipitators in which the field is fairly constant except near the central wire.

If both dust and ions are present, the field is again modified and may be expressed in the form

$$E^2 = \frac{C^2}{r^2}\varepsilon^{2PSr} + \frac{\lambda i}{K}\,\frac{\varepsilon^{2PSr} - 1 - 2PSr}{P^2S^2r^2}$$

or

$$E^2 = \frac{C^2}{r^2}(1 + 2PSr + 2P^2S^2r^2 + \ldots) + \frac{2\lambda i}{K}(1 + \tfrac{2}{3}PSr + \tfrac{1}{3}P^2S^2r^2 + \ldots) \quad (7)$$

where S is the total outer surface area of dust per unit volume of gas.

Again, if ionization is very intense, so that $C^2/r^2 \ll 2\lambda i/K$ over the greater part of the inter-electrode space,

$$E = \sqrt{\frac{2\lambda i}{K}}\left(1 + \frac{PSr}{3}\right) \text{ volts per metre} \quad . \quad . \quad (8)$$

(2.4) Charge Carried by the Dust Stream

It may be shown (see Section 10) that the current, I_d, constituted by a flow of charged dust particles is determined by

$$I_d = \frac{PSW}{4\pi\lambda}\sqrt{\frac{2\lambda i}{K}} \text{ amperes} \quad . \quad . \quad . \quad . \quad (9)$$

in M.K.S. units.

This equation reduces to

$$I_d = \frac{A\sqrt{i}}{1\,800\pi} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (10)$$

if $K = 1{\cdot}8 \times 10^{-4}$ m/sec per volt/m and $P = 2$ (these being typical values for flue gases), and if $A = SW$ square-metres per second. The surface area of dust travelling in a gas stream is then

$$A = \frac{1\,800\pi}{\sqrt{i}} I_d \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (11)$$

The reading given by the smoke monitor is thus proportional to the total surface area of dust per second passing through the inlet nozzle. It is also proportional to the square root of the corona current. However, experiment has shown that, as i is increased, I_d increases to a limit where saturation occurs, and further increases in i have little effect on I_d. The smoke monitor should therefore be operated with i above this saturation value, but below the point of flashover.

Clearly, the charge carried by a dust particle cannot exceed the value which would cause breakdown of the gas at its surface. Thus the upper limit of field at the surface of the particle is 3 MV/m, and so Q_{max} is $3 \times 10^6 r_a^2/\lambda$. However, if every particle were carrying this charge, the calculated value of I_d would be larger than is found in practice.

(2.5) Particle Charging Time and Required Ionizer Length

The time for a spherical particle to acquire half its limiting charge is given by

$$T_0 = \frac{4\epsilon}{eN_0K} \text{ second} \quad . \quad . \quad . \quad . \quad . \quad (12)$$

and is independent of particle size. With mean concentration of the order of $N_0 = 10^{15}$ ions/m^3, T_0 is about 2 millisec.

The length of the charging section must be such that, at the maximum anticipated gas velocities, the particles remain in the ion space-charge sufficiently long to acquire a substantial fraction of their limiting charges, but not to be precipitated on the tube walls. To quote figures used in the design of the prototype monitor, with a gas velocity of 40 ft/sec through a 1 in-diameter sampling nozzle, the particle velocity in the ionizer is 10 ft/sec. Thus the distance travelled by a dust particle in 2 millisec is 0·24 in; the discharge wire should not be shorter than this, and a length of about 0·5–1·0 in is practicable.

(2.6) Particle Drift Velocity and Collector Length

The drift velocity of a charged particle across the inter-electrode gap may be estimated by equating the force of attraction on the particle due to the collector field and the viscous drag of the gas. If the particle is carrying its limiting charge, $Q_0 = PEr_a^2/\lambda$ in a field E_p, the electric force is given by

$$F_1 = \frac{P}{\lambda}E_pEr_a^2$$

By Stokes's law, the viscous drag is

$$F_2 = 6\pi\mu r_a v$$

Under steady conditions $F_1 = F_2$, and therefore the drift velocity is

$$v = \frac{PEE_pr_a}{6\pi\mu\lambda} \text{ metres per second} \quad . \quad . \quad . \quad (13)$$

The required length of the collector section may then be calculated from v and the gas velocity for the anticipated range of particle size.

This theory, however, is incomplete. If the gas flow were laminar, the particle trajectory would be parabolic; in fact, flow is turbulent, and the particle path is governed by the motion of the gas and perhaps by electric wind effects. Each particle follows its own path, its drift velocity varies as it moves to different parts of the field and the charge it carries is determined by the path it has followed in the ionizer. The best collector length is found from practical experience and experiment, and is generally shorter for small particles than the value found from the above theory.

(2.7) Suppression of the Corona Discharge

Dust present in the ionizer inter-electrode gap constitutes a negative cloud, reducing the field strength around the discharge wire and hence lowering the corona current. Pauthenier and Moreau-Hanot have studied this effect and have shown that the corona current is reduced by a factor

$$\frac{i}{i_0} = 1 - \frac{PSR_0}{3} \quad . \quad . \quad . \quad . \quad . \quad . \quad (14)$$

where i is the corona current with, and i_0 the current without, dust, both per unit length of discharge wire; the reduction is marked when there are a large number of small particles present.

Conducting dust, however, may cause an increase in the measured corona current, possibly because of discharge between individual particles. This effect was observed when finely divided graphite was used as an entrained dust during tests on the experimental smoke monitor.

(2.8) Particle Charging by Ion Diffusion

Ions present in a gas participate in the thermal agitations of the gas molecules, and their kinetic energy for this motion is equal to that of the molecules of the gas in which they are found. If dust is also present, collisions with ions occur which result in an accumulation of charge on the particles, and the charging rate gradually decreases as the coulomb repulsion force grows. The effect was studied by Arendt and Kallman[5] and others in Germany; it becomes important for particle diameters below $0\cdot2\,\mu$, and occurs entirely independently of any applied electric field.

If a particle is among gas ions and has already acquired some charge, the number, n, of elementary charges captured after a time t seconds can be determined from the following equation given by White,[7] which assumes that all collisions are effective:

$$n = \frac{r_a kT}{e^2} \log_\varepsilon \left(1 + \frac{\pi r_a v_c N_0 e^2 t}{kT}\right) \quad . \quad . \quad . \quad (15)$$

In the apparatus illustrated in Fig. 1 the voltages applied to the discharge wire and the collector electrode were independently variable, and the effective length of the ionizer (and hence the time of exposure of the particles to the corona) could be varied by sliding the tube B over the discharge wire.

A flow of room air with entrained cigarette smoke was maintained through the apparatus by means of a suction fan, and the current flowing to the collector in the precipitating section was measured under various conditions. Some results obtained are shown plotted in Figs. 2–5.

Fig. 2 shows how the collection efficiency increases with increasing collector voltage and gradually flattens off as all the particles are caused to strike the outer electrode.

Figs. 3(a) and (b) indicate that the dust current increases with the time the particles are exposed to the ion cloud; again, the curves flatten, particularly at the reduced air flow in Fig. 3(b), as

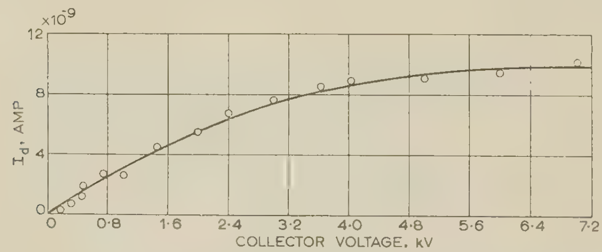

Fig. 2.—Variation of I_d with collector voltage for entrained cigarette-smoke particles.

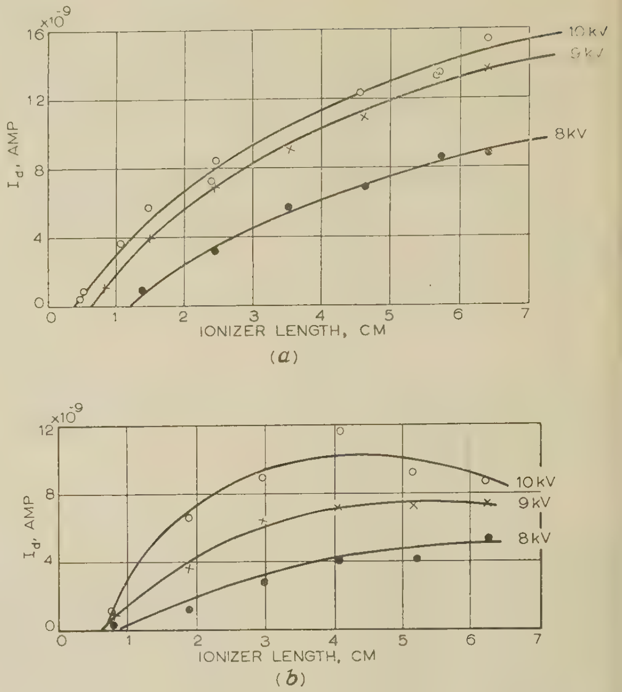

Fig. 3.—Variation of I_d with ionizer length for entrained cigarette smoke, with discharge-wire potentials of 8, 9 and 10 kV and a collector potential of 5 kV.

(a) Air flow = $4\cdot1\,ft^3/min$.
(b) Air flow = $2\cdot6\,ft^3/min$.

the particles become charge-saturated. With a long ionizer and high discharge-wire potential, some of the particles never reach the collector section.

In Figs. 4 and 5 the ionizer voltage and ion current are varied and curves are drawn for three collector voltages; the corona-onset voltage was about 6 kV.

Let the following values be inserted in eqn. (15):

$$r_a = 0\cdot5 \text{ and } 0\cdot05\,\mu$$
$$k = 1\cdot38 \times 10^{-16}\,\text{erg/deg}$$
$$T = 293^\circ\,\text{K}$$
$$e = 4\cdot8 \times 10^{-10}\,\text{c.g.s. e.s.u.}$$
$$v_c = 55\,\text{km/sec (say)}$$
$$N_0 = 4 \times 10^9\,\text{ions/cm}^3\text{ (estimated)}$$

Then $n = 8\cdot9 \log_\varepsilon (1 + 194\,000t)$ for $r_a = 0\cdot5\,\mu$

$= 0\cdot89 \log_\varepsilon (1 + 19\,400t)$ for $r_a = 0\cdot05\,\mu$

If $r_a = 0\cdot05\,\mu$ and $N_0 = 4 \times 10^8\,\text{ions/cm}^3$

$n = 0\cdot89 \log_\varepsilon (1 + 1\,940t)$

In Fig. 6, n is shown plotted against t for these three conditions and the shape of the curves may be compared with those of Figs. 3(a) and (b). With an air flow of $4\cdot1\,ft^3/min$ the particles travelled through 1 cm of ionizer length in $0\cdot004$ sec. Since the maximum length of the ionizer was about 6 cm, the maximum time the particles were subjected to the charging field at this air flow was $0\cdot024$ sec, not allowing for air turbulence; neither the experimental nor the theoretical curves become quite horizontal within this time interval, indicating incomplete charging of the particles. But with a 6 cm ionizer and an air flow of $2\cdot6\,ft^3/min$, the particles were charged to their limiting value as determined by the ion density. These results appear to be in accordance with the ion-diffusion theory.

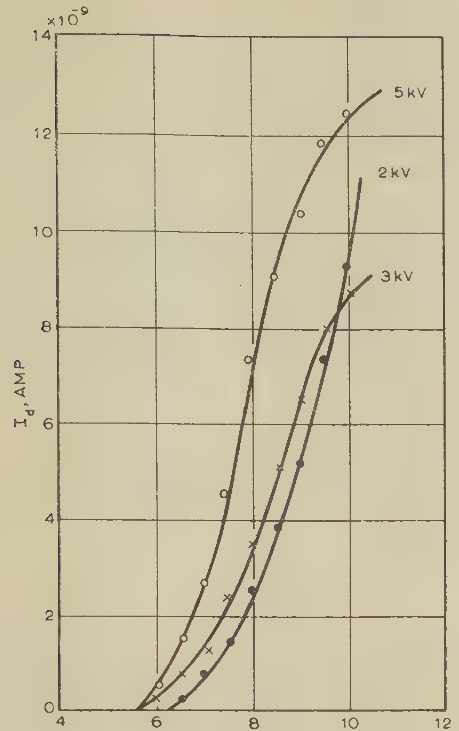

Fig. 4.—Variation of I_d with ionizer voltage for collector voltages of 2, 3 and 5 kV.

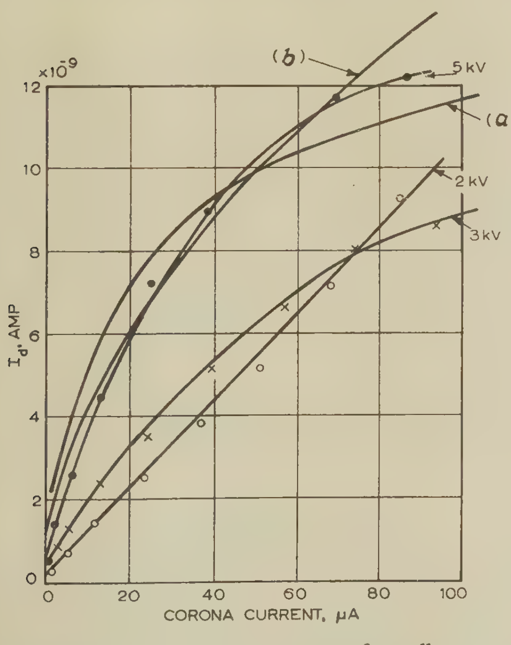

Fig. 5.—Variation of I_d with corona current for collector voltages of 2, 3 and 5 kV with two theoretical curves based on (a) the ion-diffusion theory and (b) the ion-bombardment theory with $I_d \propto \sqrt{i}$.

By the ion-bombardment theory, the particle time-constant, τ_0, with $N_0 = 4 \times 10^{15}$ ions/m^3 and $K = 1 \cdot 8 \times 10^{-4}$ m/sec/volt/m in clean room air is $0 \cdot 3$ millisec, so that the particles acquire 91% of their limiting charge in 3 millisec. This gives a higher charging rate than was observed in these cigarette-smoke experiments.

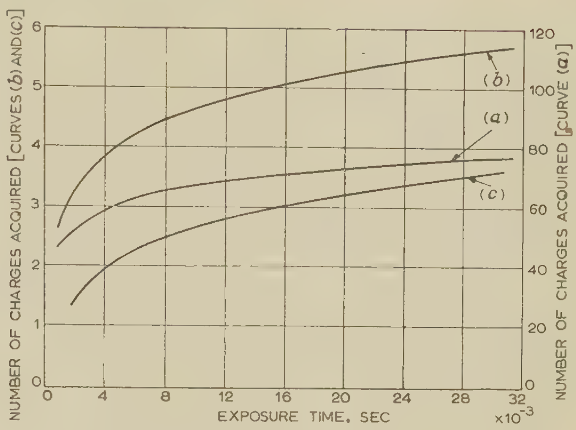

Fig. 6.—Curves calculated from the ion-diffusion formula.

(a) $r_a = 0 \cdot 5\,\mu$; $N_0 = 4 \times 10^9$ ions/cm^3.
(b) $r_a = 0 \cdot 05\,\mu$; $N_0 = 4 \times 10^9$ ions/cm^3.
(c) $r_a = 0 \cdot 05\,\mu$; $N_0 = 4 \times 10^8$ ions/cm^3.

By the ion-diffusion theory, particle charging varies with corona current according to a logarithmic function. In the ion-bombardment process it was shown that particle charge is directly proportional to the square root of the current. Fig. 5 shows two theoretical curves, but it should be remembered that theory does not account for collector efficiency, which is reduced as the particle charging falls. The curve based on the bombardment theory obeys the law $I_d =$ constant $\times \sqrt{i}$, the constant being chosen so that the graph passes through one of the experimental points. A similar procedure was adopted for the ion-diffusion curve, since the number of particles passing per unit time was difficult to assess.

(3) SAMPLING FLUE GASES

(3.1) The Apparatus

A dust monitor embodying the principles described was constructed in order to obtain site experience in sampling flue gases from a pulverized-fuel-fired boiler. This was designed for immersion in a chimney duct, as shown in Fig. 7, but to ease

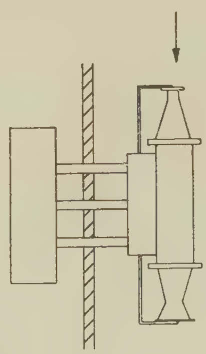

Fig. 7.—Dust monitor completely immersed in the chimney gases, counterbalanced by control box.

installation difficulties it was found preferable to mount the monitor outside the duct and add sampling and return pipes which projected through the duct wall, as illustrated in Fig. 8

Tests in a wind tunnel revealed that, with correct aerodynamic design, a fraction of the main gas-stream would pass through the monitor over the anticipated range of flue-gas velocities without the need for a suction fan. Sampling would be sub-

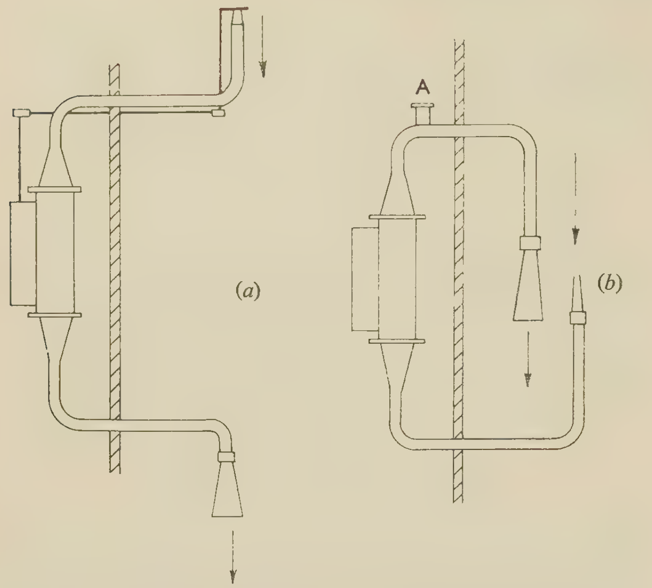

Fig. 8.—Monitor mounted outside the duct.

(*a*) Sampling tubes projecting into the gas stream.
(*b*) Inlet and discharge ends of the sampling pipes brought together to avoid differences in gas velocity and static pressure.
The scavenging air inlet is shown at A.
The arrows indicate the direction of the flue gases.

stantially isokinetic, i.e. the velocity of the gas entering the nozzle would be equal to that surrounding it, so ensuring a minimum disturbance of the flow pattern.

This early model incorporated remotely operated flaps for closing the ends of the apparatus against entry of dust during non-sampling periods, together with a rapping mechanism for displacing dust deposited on the collector. Provision was made for measuring the sampling rate, gas velocity in and around the sampling nozzle, pressures and temperatures. Electrical heaters were added to the main body of the monitor and to the pipework outside the duct to prevent gas cooling and condensation.

When the equipment was first installed, it was apparent that the sampling rate predicted by the wind-tunnel trials could not be achieved, owing to turbulence in the duct. Exploratory tests with a pressure and pitot probe, however, led to the satisfactory arrangement shown in Fig. 8(*b*), in which the inlet and outlet are close together to avoid differences in duct pressure and gas direction. A larger outlet cone was also fitted to increase the sampling rate, and the outlet flap was omitted.

The apparatus was arranged to record automatically so that it could be left unattended for a week or more. To reduce blockage by deposited dust, sampling at this stage was permitted for only a few seconds at preset intervals. To achieve this, a control circuit was designed, initiated by a time switch, to energize the high-voltage supplies, open the inlet nozzle, rap the collector electrode, purge the system with an air-blast, and record for 35 sec. The inlet nozzle was then closed, the high-voltage supplies were turned off and the circuit was reset for the next operation. To avoid transients, the amplifier input was earthed as the h.t. unit became energized. Deposited dust, if dry, could readily be blown out of the equipment by a sudden rush of air. No air supply was needed, since the duct pressure was below ambient, and atmospheric air was admitted by a simple solenoid-operated butterfly valve; its position was later modified by experience.

In a later series of tests the equipment was allowed to record continuously, the control circuit then doing no more than admit cleaning air for a short period at 20 min intervals.

(3.1.1) The H.T. Unit.

The negative potentials for the ionizer (10 kV) and the central collector electrode (5 kV) were obtained from a high-voltage transformer feeding both a voltage doubler and a single rectifier-capacitor arrangement.

(3.1.2) The Amplifier.

To avoid developing a special amplifier for this work, one already available was used after a slight modification.

The requirements to be met are as follows:

A d.c. input of 10^{-6}–10^{-8} amp full-scale deflection in three ranges.
Negligible drift over periods of a month or more.
Reliability and robust construction, suitable for ambient temperatures up to 40° C.
Output sufficient to drive a robust pen recorder, i.e. not less than 1 mA.
Input impedance not greater than about 1 megohm.
Rapid response time not essential.
Suitable for operation with a screened input lead of up to 100 ft in length and output leads a few hundred feet long.
Ease of maintenance and provision for simple checking of zero and full-scale points.

(4) TESTS

(4.1) General

By co-operation from the C.E.G.B., initial tests were made with the smoke monitor fitted to the flue duct of No. 2 boiler at Hams Hall 'B' generating station in the manner shown in Fig. 8(*a*). The site is one generally used for flue-gas sampling and lies between the main electrostatic precipitators and the suction side of the induced draught fans. The duct is straight for some 10 ft only, there being a bend above the sampling holes and a fork below them. The duct section is approximately square, with an area of 39 ft^2.

The boiler is of the pulverized-fuel type with a maximum rating of 280 000 lb of steam per hour, and routine operations, some of which appear on the smoke-monitor recordings, include

(*a*) Starting up on fuel oil, often between 5 a.m. and 6 a.m. on weekday mornings, for a time depending upon the initial temperature of the boiler.
(*b*) Ashing out; this usually takes 30–45 min to complete and involves the removal of fused clinker from the bottom of the boiler.
(*c*) Water lancing for about 10 min per day, when a jet of water is played on the boiler tubes to break up solid deposits which impair heat transfer.
(*d*) Soot blowing for about 10 min each day.

The last three operations are usually performed in the morning between 8 a.m. and 10 a.m.

The general direction of the flue gases is downwards at the sampling point, but owing to turbulence, bending and forking of the duct, the direction and velocity of the gases at any point is extremely variable.

(4.2) Object of the Tests

The tests were made to compare readings with those given by a gravimetric method and to observe the effects of prolonged operation upon performance and the constructional materials. It was of particular interest to know for how long the monitor could operate unattended.

(4.3) Comparative Tests

The monitor sampled continuously for periods of about 4 hours in conjunction with gas-filtering equipment comprising a ¼ in diameter nozzle facing into the gas stream (the direction and velocity being determined by pitot measurements to ensure isokinetic sampling), a paper filter thimble, an extraction pump and means for determining the sampling rate. The filter thimble and tube were electrically heated to prevent condensa-

tion. After about 4 hours, depending upon the dust burden, the solids collected in the filter were weighed and the result was expressed in grains per cubic foot of gas. Owing to differences in duct static pressure and gas direction between the ends of the monitor, the sampling rate at that time was not isokinetic. Consequently, the results of these earlier tests cannot be directly interpreted. This difficulty was, as already stated, overcome by bringing the inlet and exit ends of the sampling tubes together, returning the sampled gases to the main stream through a larger cone and ensuring correct alignment of both the sampling nozzle and the cone with the gas stream, as in Fig. 8(*b*). An approximate mean value of the dust current was determined from the recorded chart.

Readings of duct temperature, temperatures in the apparatus, and gas velocities were taken and the results of the four tests are summarized in Table 1. If the second test is neglected and an average conversion factor taken from the other three tests, it is found that 0·34 g of dust sampled by the monitor per second would give rise to a current of 1 μA flowing from the collector to the amplifier.

Assuming the typical value 3 500 cm^2/g for the specific surface area of the dust, 1 μA = 1 190 cm^2/sec sampled by the 1 in nozzle.

This figure may be compared with that found by theory. It was shown that

$$A = \frac{1 \cdot 8\pi I_d}{\sqrt{i}} \text{ square centimetres per second}$$

where I_d is in nano-amperes and i in microamperes per centimetre.

If $I_d = 1\,\mu$A and the saturation corona current is 25 μA/cm, $A = 1\,130$ cm^2/sec; this is in good agreement with the test figure.

(4.4) Prolonged Operation

(4.4.1) Intermittent Recording.

The equipment was operated night and day for about a month, sampling for 35 sec at 20 min intervals, fitted as shown in Fig. 8(*b*). The control-circuit sequence, described earlier, included a blast of cleaning air lasting 30 sec while the inlet nozzle was opening. An example of the recording obtained is reproduced in Fig. 9(*a*) and the amplitude of the reading may be compared with the boiler load, also marked on the chart.

During the test the sampling rate was measured and no appreciable change was observed.

(4.4.2) Continuous Recording.

The next step was to allow continuous sampling. The cleaning-air valve opened again for 30 sec at 20 min intervals but the inlet nozzle remained permanently open. As before, no significant change in performance was observed, although dust tended to collect in the ionizer and the corona current diminished a little.

The test was continued for about 10 days and a portion of the record is shown in Fig. 9(*b*). The chart clearly illustrates rapping of the main precipitators every 15 min. The transients occurring at 20 min intervals in the weekday recording and at repetitive intervals of 10, 20 and 30 min in the weekend recording are caused by the monitor air-blast cleaning operation. Oil burning when the boiler is started up is marked by a full-scale reading which persists until the change-over to pulverized fuel is made.

Table 1

TEST RESULTS

Date of test	Boiler steam load	Dust loadings by filter probe	Flue-gas velocity	Mean dust current by monitor	Monitor sampling velocity	Sampling rate	Conversion factor
	lb/h $\times 10^3$	g/ft^3 $\times 10^{-3}$	ft/sec	μA $\times 10^{-3}$	ft/sec	ft^3/sec $\times 10^{-3}$	g/sec/μA
10.6.58	280	29	31	6·6	14·5	79	0·35
11.6.58	280	31	25	10·5	12·0	66	0·19
12.6.58	200	19	26·3	3·6	11·5	63	0·33
12.6.58	200	30	22·5	4·5	9·4	51·2	0·34

Notes.—The second test was marred by a boiler fault lasting about half an hour. Increased dust loading during the fourth test was obtained by continuously rapping the main precipitator.

(4.5) Quantity Measured

The dust monitor provides, as shown, a measure of dust burden proportional to surface area per second, so that the area under a recorder chart becomes proportional to the total dust area in that time.

From theory, a dust current of 1 μA corresponds to a dust loading of about 1 130 cm^2/sec through the apparatus. On the chart of Fig. 9(*a*), an output of 1 mA from the amplifier corresponds to an input of 0·15 μA, so that the full-scale deflection of 1 mA is equivalent to 170 cm^2/sec sampled by the 1 in-diameter nozzle. Assuming uniform dust concentration over that section of the chimney, the full scale of 1 mA equals 31 200 cm^2 of dust passing 1 ft^2 of duct per second. [A different calibration holds for Fig. 9(*b*).]

To determine the concentration of dust in the gas at any time it is necessary to know the gas velocity, which must be assumed constant over that section and equal to the sampling velocity. Neither the dust burden nor the gas velocity is by any means constant over any section of these ducts, but it is feasible to suppose that a sampling point could be chosen where fairly representative conditions prevail, even at varying boiler loads, or baffles could be used to improve the flow pattern. At the sampling point in these tests the gas velocity with the boiler steaming at full load was usually about 40 ft/sec and the sampling velocity was within a few per cent of this. For example, after a week of continuous sampling with three cleaning-air blasts per hour, the flue-gas velocity was found to be 33·7 ft/sec and the sampling velocity 32·4 ft/sec, i.e. within 4% of being isokinetic.

To compare the reading with the dust burden on the weight basis the specific surface area of a dust sample is required. This may be found roughly by the air-permeability method in which

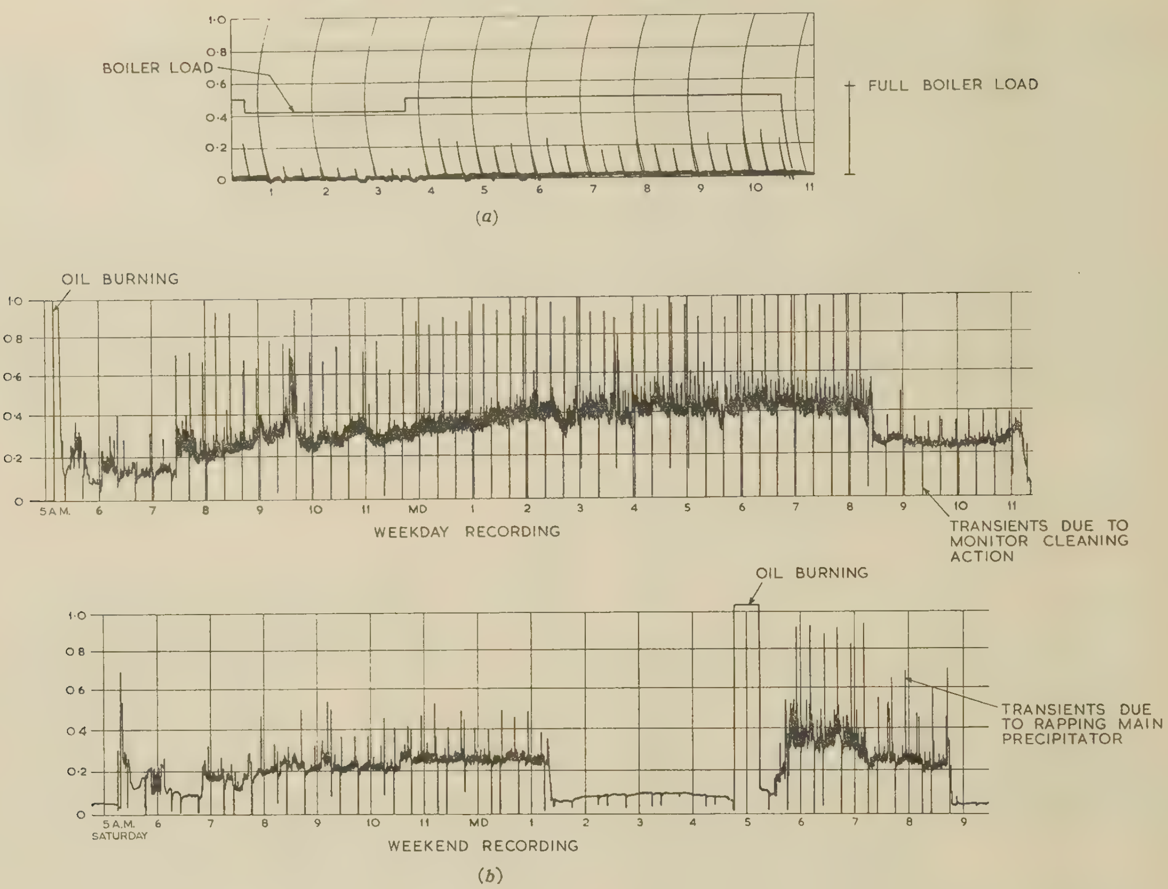

Fig. 9.—Examples of recordings obtained with the electrostatic dust monitor.

(*a*) Intermittent.
(*b*) Continuous operation.

air is passed through a bed of the dust to determine the pressure difference across it.

(5) OBSERVATIONS

(5.1) Pulverized-Fuel Ash or Fly-Ash

In a pulverized-fuel-fired boiler the coal is ground to a fine powder, about 80% of which passes through a 200-mesh sieve, and is continuously blown into the furnace and burned. A large station may burn 2000 tons of coal each day. If the ash content is 12% and 85% of it is entrained in the flue gases, 200 tons of dust and grit per day must be prevented from reaching the atmosphere. Flue-gas cleaning by electrostatic precipitation may have an efficiency of 98–99%, and so the total amount of dust reaching the atmosphere could be 4 tons per day.

The following examples of dust loadings from some generating stations have been given by Brown:[6]

Kingston 'B'	$0\cdot06$ grains/ft^3 at s.t.p.
Llynfi	$0\cdot35$ grains/ft^3 at s.t.p.
Huncoat ($99\cdot4$% precipitation efficiency)	$0\cdot0419$ grains/ft^3 at s.t.p.

Converting the figures obtained by the gravimetric method during the present tests, the dust burdens are given in Table 2.

The size of pulverized-fuel-ash particles depends upon the coal, the boiler design and the condition of the grinding mills. The following ranges are usually covered:

	Diameter
Extreme fines	$0\cdot01$–$1\cdot0\,\mu$
Medium fines	$1\cdot0$–$20\,\mu$
Large particles	20–200 μ

Surface-area figures given by Tigges and Karlsson[2] are:

Area	*Particle diameter*
$99\cdot3$%	$<60\,\mu$
$78\cdot9$%	$<10\,\mu$
$6\cdot8$%	$<1\,\mu$
$0\cdot16$%	$<0\cdot1\,\mu$

Table 2

Dust Burdens at Duct Temperature and Pressure

Date	Boiler steam load	Dust burden
	lb/h $\times 10^3$	Grains/ft^3
10.6.58	280	$0\cdot45$
11.6.58	280	$0\cdot48$
12.6.58	200	$0\cdot29$
12.6.58	200 (continuous rapping)	$0\cdot46$

Many of the particles are spherical, which tends to justify the assumed sphericity in the theory.

(5.2) Heating of the Monitor and Constructional Materials

The temperature of the flue gases at the sampling point reaches a maximum of about 140° C at full boiler load, and sampling equipment must be designed to operate at these conditions. It is more important, however, that the dust monitor be lagged and provided with heaters to maintain it at a temperature above the dew point of the gases. In the present equipment about 700 watts applied to the outside casing of the central body and the two pipe bends exterior to the duct were needed to raise them to a mean temperature of 100° C. If the apparatus is unheated, the following consequences result:

(*a*) The ash is corrosive when damp and its acid content damages the metal parts.
(*b*) Damp fly-ash increases fouling of the apparatus, and is difficult to remove by an air blast.
(*c*) Moisture impairs the electrical properties of the insulation.
(*d*) Being an electrolyte in contact with dissimilar metals moisture in the apparatus causes a battery effect, and a large unwanted signal is fed into the amplifier.

The experimental equipment was constructed mainly of mild steel and pyrophyllite insulation. No serious erosion or corrosion was apparent, except to the collector electrodes, which were chromium plated. The chromium quickly combined with sulphur in the flue gases, but could be protected by a coating of silicone varnish. In subsequent designs stainless-steel electrodes are used.

(6) RECENT AND FUTURE WORK

During the endurance tests with the monitor fitted to the duct as shown in Fig. 8(*b*), although the sampling rate was frequently checked and no apparent change observed, the electrical performance tended to deteriorate as dust built up inside and the sparkover voltage approached the corona onset voltage. When the interior of the equipment was examined, a substantial amount of dust was seen on the discharge electrode, on the walls of the charging section and on the collector. The bushings were heavily coated and the deposit appeared to be in layers separated by a black film of oil-fume particles.

In the engineered prototype of this equipment the purging air is admitted through a wire mesh which forms the outer cylindrical electrode in the charging section; recent tests have shown this to be effective in preventing corona-current failure. The constructional details of the apparatus have also been simplified, with the result that it is now less bulky and cheaper to manufacture.

(7) CONCLUSIONS

Tests so far indicate that the method should find a useful application in power-station flue-dust control. Corrosion and fouling of the sampling tubes and electrodes can be overcome by the choice of suitable constructional materials and by air-purging at regular intervals.

Experimental equipment has now been in operation continuously for several months without any significant change in performance. Further trials at other boiler installations will provide more information about the accuracy of the instrument and the consistency of its calibration under different boiler and fuel conditions.

Heating and lagging of the apparatus is important in preventing condensation of the flue gases, thus reducing corrosion, easing cleaning difficulties and preventing the generation of spurious e.m.f.'s by electrolytic action.

The dust burden is measured in terms of the total surface area of solids sampled per second, and this quantity is considered significant in the study of the harmful effects of air pollution.

(8) ACKNOWLEDGMENTS

Thanks are due to the Central Electricity Generating Board for permission to conduct tests on No. 2 boiler, Hams Hall 'B' generating station and for continued encouragement and helpful co-operation.

The author is also indebted to Mr. P. F. Griffin and other colleagues in the Research Laboratory of Associated Electrical Industries (Rugby) Ltd., for their assistance in constructing and testing the equipment.

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(10) APPENDIX

Charge Carried by the Dust Stream

Assume a number of spherical, homogeneous particles each carrying its limiting charge $Q_0 = PEr_a^2/\lambda$ flowing in a gas stream along an earthed metal tube of radius R_0 with a thin axial wire of radius r_0. Assume also that there is no loss of dust by precipitation on the electrode surfaces and that the particles are uniformly distributed in the inter-electrode space. Consider an elementary disc-shaped volume δx thick and δr wide at radius r, perpendicular to the longitudinal axis of the tube. Then the surface area of dust in the elementary volume is $2\pi r\delta x\delta rS$ and the charge contained in this volume is $PSEr\delta r\delta x/2\lambda$. The total charge in a cross-section of the tube δx thick is

$$\frac{PS}{2\lambda}\delta x\int_{r_0}^{R_0} rEdr$$

If this volume is travelling along the tube at a speed v_s, the current due to the flow of charged dust is

$$I_d = \frac{PSv_s}{2\lambda}\int_{r_0}^{R_0} rEdr$$

The expression for the field E with ions and charged dust present is

$$E^2 = \frac{C^2}{r^2}\varepsilon^{2PSr} + \frac{\lambda i}{K}\frac{\varepsilon^{2PSr} - 1 - 2PSr}{P^2S^2r^2}$$

Expressing this in the form

$$E^2 = \left[\frac{C^2}{r^2} + \frac{\lambda i}{K(PSr)^2}\right]\varepsilon^{2PSr} - \frac{\lambda i}{K}\left[\frac{2}{PSr} + \frac{1}{(PSr)^2}\right]$$

and substituting it in the above expression for I_d, integrating and neglecting small terms gives

$$I_d = \frac{PSW}{4\pi\lambda}\sqrt{\frac{2\lambda i}{K}} \text{ in M.K.S. units} \quad . \quad . \quad . \quad (9)$$

where $W = v_s\pi R_0^2$, the volumetric gas flow.

If the classical expression for E had been used the result would have been

$$I_d = \frac{PSW}{4\pi\lambda}\,\frac{2V}{R_0 \log_\varepsilon \dfrac{R_0}{r_0}}$$

DISCUSSION BEFORE THE SUPPLY SECTION, 16TH MARCH, 1960

Dr. J. S. Forrest: The power stations of this country produce some five million tons of dust a year, and it is clear that the collection of this dust and the monitoring of any dust emission which escapes the collecting plant is a major problem and one of considerable economic importance. We all welcome the Clean Air Act and wish it every success, but I am appalled at the crude, unscientific and actually misleading measuring technique on which the operation of the Act is based. The pollution caused by a source is measured by means of a subjective estimate of the degree of darkness of the smoke. Even if you accept the principle, this is a poor kind of measurement, because, as everyone knows, a light smoke will appear dark against a bright background and a dark smoke appears light when illuminated by strong sunlight against a background of dark clouds. But surely we are not concerned with the appearance of the smoke when we complain about a pollution nuisance. What we are concerned about is the dirt deposit and rapid deterioration of paint-work, the soiling of fabrics and decorations and possible health hazards. I therefore feel that we shall have to adopt a more realistic method of measuring pollution, and the paper is timely from this point of view.

The author has set himself the task of measuring the surface area of the dust—in other words, particle size—and he shows theoretically that the charge collected by the particles in an electric field is a measure of the total surface area. He also demonstrates that the instrument gives plausible readings in a power-station flue when dealing with very heterogeneous dust. But electrostatic-precipitator theory is based on many assumptions, and I should have found the claims in the paper more convincing if the instrument had been calibrated using homogeneous dust particles of known surface area, e.g. graded Perspex spherical particles. Has the author made any experiments of this kind?

However, if we accept the conclusion that the instrument gives a true measure of the total surface area of the dust, it is then difficult to see why it has advantages over the various existing instruments which measure the optical obscuration of a light beam by the dust. These instruments give readings proportional to the surface area, bearing in mind that in power-station pollution problems we are not concerned with submicron sizes. Will the author comment on this point? Incidentally, one of the major difficulties with optical-density-type instruments—the tendency for windows to become dirty—has recently been overcome by a device developed at the Central Electricity Research Laboratories (C.E.R.L.).

A minor point is that Section 5.1 implies that Kingston 'B' is a pulverized-fuel station: it is, in fact, a chain-grate stoker-fired station.

Summing up, I would say that all our experience with air pollution around power stations and other industrial chimneys leads us to believe that where serious complaints are made they are caused by the fact that the relatively coarse dust has impinged or settled and has thereby made dwellings, plants or furnishings dirty. At C.E.R.L. we have for some time been working on the design of an instrument which would give a more realistic measure of the nuisance caused by pollution; this instrument measures, in effect, the dirtiness of a surface exposed to the pollution. In addition, the sensitivity of the instrument has been deliberately increased for coarse dust and reduced for fine dust, and this should give power-station operators a dependable idea of the nuisance value of the dust which is being emitted. The instrument has been installed in several power stations and a brief description of it will be given later in the discussion.

Dr. L. Cohen: As a general point, I think that the author is rather misguided in putting so much emphasis on theoretical work. Experience with electroprecipitators shows how inadequate are theoretical interpretations in this subject.

There are a number of points on which I should like the author to comment. Corona quenching is mentioned briefly in the paper: this can reduce particle charging very strongly, particularly when very fine dust or mist is present.

The problem of high-resistivity dust is unfortunately not limited to p.v.c. dust and is, in fact, often encountered in modern power stations with very high combustion efficiencies and particularly with low-sulphur coals. One effect associated with high resistivity is back-discharge, which reduces collection efficiency. This is known to occur with very thin layers of dust, so that the instrument might be affected quite quickly. Another effect of high resistivity, which may occur in the collecting section of the instrument, is to restrict the rate at which the charge escapes.

The author quotes typical values of specific surface without comment, but there must be involved some assumption as to the shape of the particles and their size distribution. These, of course, are not constant from one installation to another.

An important aspect of the application of the instrument is that it takes a sample of the material at a single point. I think that it is highly unlikely that anything like a uniform concentration could be obtained in a duct, with or without mixing baffles. When dust-burden measurements are made by gravimetric methods, samples are taken at a number of points to give a mean value. This reinforces Dr. Forrest's remark about the use of the optical method, which gives a value across the full section of the duct.

Cleaning by an air blast is probably quite effective for most dusts, but certain flue dusts are known to form adherent deposits which are very difficult to remove, even mechanically.

Finally, can the author comment on the German *Konitest* equipment, which is similar to his except that charging is by friction and not by corona discharge?

Mr. D. H. Lucas (*read by Mr. W. L. Snowsill*): The instrument developed at Central Electricity Research Laboratories was

designed to take account of the way in which dust causes nuisance, rather than to make any arbitrary measurement like the weight of dust emitted. It is known that nearly all the complaints about dust arise because it first of all settles or impinges on some object and then obscures light which would otherwise fall on the object, i.e. it makes things dirty. The importance of settlement is obvious, because dust which remains at the level of the chimney top cannot affect the cleanliness of houses at ground level. Even when the dust approaches the ground it has an effect only if it is removed from the air stream by some form of impingement. Thus, trees become dirty by removing dust from the air, grit enters the eyes by a form of impingement, and grit enters houses only if it is thrown forcibly through slightly open windows. The deposition of dust in this sort of way is considered offensive because it has a visible effect on otherwise clean surfaces.

These facts have determined the form of a sampling head of the instrument. It contains a small nozzle, but no gases are drawn through the nozzle, as in the author's instrument; the gas velocity at the nozzle is zero. However, the particles of dust passing along the duct impinge on this nozzle, and the coarser the particles are, the more likely are they to enter the nozzle. This corresponds to the first process of impingement or settlement mentioned previously. After they have entered the nozzle, the dust particles settle on a transparent glass surface, and after a preset collecting time, the dirtiness of this glass is measured with an optical system and photocell: this corresponds to the second part of the process of causing nuisance. After the measurement is taken, the glass is cleaned by a blast of compressed air. The measuring system has been carefully designed to cancel out errors due to changes in supply voltages, changes of temperature, and changes of lamp or photocell with age.

The readings obtained by the instrument are shown by lines drawn on a circular chart. The length of the line drawn at the end of a 15 min period shows the nuisance value of the emission during the preceding 15 min. We know by experience that the readings obtainable under a variety of power station conditions cover a range of at least 1 000 : 1. This range cannot be covered by changes in the length of the recorded line alone, and the instrument has been designed so that, if the emission becomes very heavy, a record is made more frequently than every 15 min and, in extreme cases, at least ten full-scale signals can be given during a 15 min period instead of the normal one. The true reading for the period is the sum of all these signals. We also know from experience that high readings are sometimes associated with a visible plume, but often occur when the plume is not noticeably dirty. It is therefore unsatisfactory to rely on smoke-density instruments or those of the type designed by the author to give warning of coarse and potentially offensive emissions.

Mr. J. Dalmon: The last column of Table 1 gives a factor relating the dust-monitor current to the weight of dust sampled per second. This factor is based on the dust burden in a flue duct as measured with the aid of a filter probe sampling isokinetically, and on the simultaneous current arising from the dust collected by the monitor which was withdrawing gas at somewhat less than half the isokinetic velocity. The errors due to non-isokinetic sampling have been investigated by Hemeon and Haines,* and from their experimental data it seems probable that the weight of dust collected by the monitor was on the low side, probably by about 40%, and mainly deficient in fines. This would reduce the conversion factor given in Table 1 from an average value of 0·34 to about 0·2 g/sec/μA. With this new value and the author's value of 3 500 cm^2/g for the specific surface area of pulverized-fuel dust, 1 μA would be equivalent to about 700 cm^2 of dust surface per second compared with the figure of 1 190 given.

* HEMEON, W. C. L., and HAINES, G. F.: 'The Magnitude of Errors in Stack-Dust Sampling', *Air Repair*, 1954, 4, p. 159.

The instrument calibration also depends on the figure of 3 500 for the specific surface area of the dust. As the author has pointed out, the actual value depends on the coal burnt, mill condition, boiler load, etc. We have calculated a few values for pulverized-fuel ash collected from a precipitator outlet at Croydon 'B' power station, and the results range from 2 500 to 5 900. The lower figure would probably be more appropriate in the present calibration because of the deficiency of fine particles, and this would reduce the value of surface area per second still further.

In our own experiments with electrostatic precipitators we found that, once a charge is imparted to the dust, a large proportion is quickly precipitated, even on insulator surfaces. Thus it is difficult to ensure that all the dust is collected on the measuring electrode. We have also found that spurious currents are sometimes measured in the collecting zone, owing to contamination of the high-voltage electrode by large particles, flakes of scale, etc. If such effects are present in the monitor, it will not indicate reliably over long periods of continuous operation—and this is an essential requirement for this type of instrument.

Mr. J. S. T. Looms: It is not quite clear from the paper whether the experimental results are considered to justify the statement that the final charge on a dust particle is proportional to the square of its radius, and therefore that a measurement of dust current gives an indication of the surface area of the dust. From the diagrams of the apparatus it is clear that considerable precipitation would be expected in the ionizing section; this is borne out by the author's statement that fouling occurred there during tests. Ionizing and dust currents are therefore inseparable.

Commenting on the theoretical Sections, I consider that the concept of a critical field intensity of 3 MV/m, above which a discharge occurs, is an over-simplification. As would be expected from Paschen's law, a higher stress is needed for fine wires than for thick ones. Thus in eqn. (2) E_s should be not a constant, but a function of inner-conductor radius.

I agree with Dr. Cohen that the theory of electroprecipitation is unsatisfactory. Two reasons are that Pauthenier's analysis assumes that no further charging occurs when there is no resultant electrostatic attraction between ion and particle, which may not be the case for ions with kinetic energy, and also that uniform current density exists along the wire, whereas emission in real precipitators is discontinuous.

Mr. W. A. Clement: None of the dust monitors available, from the simple optical devices to the latest equipments, give dust concentration in grains per cubic foot—the basis accepted by most authorities, including the Alkali Inspectorate. The only reliable method of arriving at such figures at present is by multi-point isokinetic sampling and dust extraction, and this is impracticable as a means of large-scale monitoring. If we must accept dust monitors which indicate trends only and follow with precise testing where required, it is essential that the equipment should be both simple and reliable, necessitating a minimum of maintenance. It is fair to say that a monitor of the type described by the author has worked well at Hams Hall 'B' station for several months with virtually no maintenance.

We have carried out comprehensive tests to correlate monitor records with boiler operation. Perhaps the most interesting findings are:

(*a*) The equipment appears to be most sensitive at high boiler loads.

(*b*) The equipment appears to be more sensitive to changes in gas volume than in fuel quantity.

(*c*) Operation of rapping gear in the final chamber of the precipitator is always recorded, that in earlier chambers only occasionally.

In general, the reaction of the monitor to short-term transient conditions is very impressive.

We are still hopeful that for a given type of boiler burning a standard range of coals it will be possible to demonstrate some approximate relationship between monitor reading and grains per cubic foot, and trials for this purpose will shortly be carried out.

Mr. W. L. Snowsill: Mr. Clement comments that no continuously recording dust monitor or smoke-density meter at present available gives an answer in terms of the number of grains of dust per cubic foot of gas emitted, while this is invariably the figure that the Alkali Inspectorate demands when investigating a complaint. The number of grains per cubic foot is of technical interest only because it is used by convention to measure the efficiency of dust arresters. The alkali inspector's interest is to see that the nuisance is kept below a certain maximum, and since this nuisance is dependent on the particle size, the weight per cubic foot is incomplete evidence. If all the dust emitted was extremely fine, the nuisance would be far less than that caused by an equal weight of coarse dust. In addition, it is a well-known fact that when the loading on a boiler increases the volume of emitted gases increases, and therefore the amount of emitted dust. Assuming the possibility of keeping the dust concentration and grading constant, doubling the gas velocity would double the nuisance, but the number of grains per cubic foot would be unchanged. It would be far better for the alkali inspector to adopt an arbitrary unit of nuisance, measured on an instrument which responds to the rate and quality of emission, rather than an instrument which records concentration in grains per cubic foot.

It has been suggested that the author's instrument is not very sensitive to a change in boiler load at low loads, but is highly sensitive at high loads. Surely it is not the sensitivity of the instrument which increases, but the quantity of dust, which is not directly proportional to the load on the boiler. From experience gained with the C.E.R.L. dust monitor mentioned earlier it was found that the nuisance can increase by a factor of between 2 and 10 when the loading increases from 85 to 100% of full load.

Mr. F. H. E. Myers (*communicated*): Dust-monitoring devices are provided for detecting dusts which cause nuisance, and any such device should be capable of detecting every particle under any combustion condition in the boiler furnace. With saturation ionization of a sampled gas it is possible to obtain a measurement which corresponds to total particles in the sample. An electrostatic dust monitor complies with this requirement provided that the gas effluent is turbulent enough to give a representative sample.

It is the largeness or the area covered by dust which causes complaint, although the author has given other reasons. It is therefore the larger sizes of particle, whether discreet or agglomerate, which cause trouble, and a dust monitor must be capable of detecting them all.

For smoke to appear a dark shade there must be a very large number of particles per unit volume, because obscuration necessarily depends on optical density. In addition, the colour of the dust must also affect obscuration, since it is purely an optical effect. A smoke could appear to be very dark or black yet it may consist of very fine particles which may be buoyant enough to be lifted high into the atmosphere and disperse. On the other hand, a furnace could be so operated as to cause grits and dust in the effluent to be invisible and an optical monitor would therefore be of little use.

A novel type of optical densitometer has been devised, but even with this one cannot be sure that a representative sample is deposited on the glass.

The author and many of his colleagues have taken much trouble in developing the electrostatic dust monitor, which has many advantages over other types of dust-detecting device, particularly its capability of remaining in effective service for long periods.

THE AUTHOR'S REPLY TO THE ABOVE DISCUSSION

Dr. D. H. Grindell (*in reply*): It is true that electro-precipitator theory involves many assumptions, some of which are not always justified. The formulae are therefore only approximate, and calibration of the dust monitor under any particular circumstances should preferably be by experiment.

Tests to verify the statement that particle charge is proportional to surface area were made by using graded, homogeneous powders such as the Perspex spheres mentioned by Dr. Forrest. Various problems arise in such experiments; for example, a method of feeding the powder into the airstream at a constant rate and with minimum particle aggregation and minimum frictional charging must be devised. Nevertheless, in my experience, the formulae relating to particle charging are reasonably accurate, and the predicted results, although often greater than the experimental readings, were never in error by more than a factor of 2 and were more often in closer agreement than this.

I find the mathematical treatment far less satisfactory for dealing with the precipitation of particles once they have been charged; it is preferable not to attempt to calculate the drift velocity, for example, because it does not take account of possible gas turbulence and electric wind effects. I have included most of the relevant theory in the paper to complete the subject, but the design of precipitators must still be based to some extent upon practical experience.

Dr. Forrest asks what advantages this type of dust monitor has over existing optical instruments, since both measure the surface area. The main features are that the electrostatic monitor is free from the danger, present in an obscuration device, that it may indicate fouled windows or require frequent attention to avoid this. Its sensitivity extends to the invisible sub-micron particles and it is these that are likely to cause an industrial fog.

It responds to rapid transient changes in dust loading and it will run for long periods without attention. Moreover, it measures the dust sampled per second, rather than the dust per unit volume of gas, which, as Mr. Snowsill agrees, is a more truthful measure of nuisance.

The question of corona quenching is raised by Dr. Cohen. This certainly reduces particle charging and occurs when fine particles, such as smoke, mist or certain fumes, are present. In our experiments we have always recorded the charging current but have not experienced this problem in coal-burning stations during tests on the prototype monitor, which has a short ionizer and needle-shaped discharge electrode. The phenomenon was observed in earlier experiments using apparatus similar to that sketched in Fig. 1; this had a wire discharge electrode of variable effective length, and suppression became more apparent as the ionizer length was increased in an attempt to ensure complete charging of fine smoke particles.

As an example, with $9\cdot6$ kV on a $0\cdot0124$ in diameter nickel-chromium wire $2\cdot5$ in long with a 2 in diameter outer electrode, the corona current with no airflow through the apparatus was $89\mu A$. With $4 ft^3/min$ of room air flowing it rose to $93\mu A$, and when this air was mixed with the smoke from a cigarette the current fell to $87\cdot5\mu A$. When a cloth soaked with trichlorethylene was held over the inlet nozzle the corona current

fell almost to zero, and similar effects were observed with water droplets. Powdered graphite particles, however, caused an increase in corona current.

To reduce the possibility of discharges occurring in the collector, also mentioned by Dr. Cohen, and spurious currents, referred to by Mr. Dalmon, due to contamination of the collector high-voltage electrode, the precipitating field strength is maintained at a low value. Thus, with 4 or 5 kV on a 1 in-diameter inner electrode and with a 2 in-diameter outer electrode, we have as yet experienced no difficulties with high-resistivity dust in power stations.

Both Dr. Cohen and Mr. Dalmon comment upon the value of specific surface area quoted in the paper. The figure of 3 500 cm^2/g was the mean of a number of tests on dust samples taken from a monitor operating in a particular flue duct. I know of no completely satisfactory method of determining the specific surface area of collected dust, and the heterogeneous, hygroscopic dust from boiler flues is especially troublesome to measure. The air-permeability method was used in these tests because of its simplicity and because an apparatus was readily available. The technique involves compressing the dust into a bed of known dimensions, and the result depends to some extent upon the degree of compression; it is assumed that the surface area of the dust as it is in the flue is unaltered by compacting it in this way.

I should like to point out that calibration of the monitor does not depend upon the figure 3 500 cm^2/g quoted in the paper. An appropriate value of specific surface area must be found for each boiler installation if a gravimetric reading is required.

I agree with Dr. Cohen that it is difficult to find a sampling point in a flue duct where uniform conditions prevail, since gas turbulence depends not only upon duct design but also on the velocity of the gas and therefore upon boiler load. A Pitot probe should show whether there is an optimum position, and the monitor, when installed, has a limited traverse of about 3 ft back and forth across the duct. Obscuration methods have the advantage that the beam of light traverses the whole flue, but even so, its sectional area is usually small compared with that of the duct.

We have not yet encountered flue dusts which cannot be sufficiently well cleaned off the electrodes by scavenging air at a pressure of 10 in water admitted through a 2 in diameter valve for a period of about $\frac{1}{2}$ min. It is essential, however, to keep the dust dry by heating the monitor adequately, both when it is operating and when the boiler is off load.

Dr. Cohen's final question concerns German dust monitoring equipment using frictional charging.* I believe that frictional charging, particularly of a mixed dust, is less predictable than corona charging. Particles sometimes acquire positive and sometimes negative charges; some smokes are not readily charged at all by friction. Corona is very effective and fairly easy to control, whereas the degree of frictional charging depends upon the nature both of the particles themselves and of the surfaces with which they are in contact as well as upon the velocity of the dust as it passes through the charger.

The C.E.R.L. instrument described by Mr. Lucas is interesting and some convincing results have been obtained. I do not agree, however, that finer dusts can be entirely neglected, even if they are not responsible for many complaints. If they do not aggregate and settle after leaving the chimney, they continue to constitute a hazard in fog.

Mr. Dalmon correctly points out the errors which can be introduced by non-isokinetic sampling. It is hoped that further tests on the monitor can be conducted under fairer conditions.

Both Mr. Dalmon and Mr. Looms raise the question of precipitation in the ionizer. In the prototype monitor this section is short and only a small fraction of the total dust sampled collects there between cleaning air-blasts. Any dust deposited between the inlet nozzle and the collector is certainly not recorded, but samples of such dust have been observed to contain a majority of large particles which tend to contribute only a small amount to the total surface area measured.

The first paragraph of Section 2.1 may have been misleading. The critical value of field strength, E_s, depends, as Mr. Dalmon correctly points out, upon the discharge wire diameter in the case of thin wires. It is also found to depend upon the condition of the wire surface. Thus, E_s in eqn. (2) is not constant but is as defined empirically by eqn. (1).

Mr. Clement's comments that the monitor reliably reflects changing conditions in the flue gas and works for long periods without maintenance are very encouraging. We await with interest the results of his proposed tests.

* FEIFEL, E., and PROCHASKA, R.: 'Neues Elektrostatisches Staubgehalts-Meßgerät', *Zeitschrift des Vereins Deutscher Ingenieure*, 1955, 97, No. 4, p. 113.

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THE TWO-PHASE INDUCTION MOTOR USED AS A SERVO MOTOR

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SUMMARY

The performance of a 2-phase induction motor used as a servo motor depends upon the alteration of the machine from 2-phase to single-phase operation. This can be effected in three ways. Two depend upon change of applied voltage and the third depends upon alteration of the relative disposition of two stators in a specially constructed machine.

The speed/torque relationship for the three conditions is deduced from the initial assumption that the flux-density distribution is sinusoidal in space and time. The analysis reveals that single-phase motors have a maximum speed dependent upon the rotor resistance/reactance ratio as well as the frequency. It suggests a more appropriate definition of synchronous speed than that commonly accepted, namely as that speed at which the unidirectional torque becomes zero.

Theoretical performance curves of the machine as a 2-phase motor, as a single-phase motor and in the intermediate condition between these two extremes are given, for various assumed resistance/reactance ratios, as a function of the parameter which alters the machine from 2-phase to single-phase operation. Comparison between theoretical and experimental curves is made.

The resistance/reactance ratio of the 2-phase servo motor, required for effective speed control near zero speed, is demonstrated by the curves.

LIST OF SYMBOLS

α = Angular spatial displacement between stator phase windings 1 and 2.
B = Flux density at a point P in the air-gap.
B_g = Maximum value of sinusoidally distributed flux density in the air-gap.
B_m = Instantaneous maximum value of sinusoidally varying flux density.
ψ = Phase displacement between phase voltages.
$a = B_{2m}/B_{1m}$ = Ratio of magnitudes of maximum flux densities.
$\omega = 2\pi f$ = Angular frequency of supply voltage.
n = Angular velocity of rotor conductors.
$\omega_r = 2\pi n$.
$\theta = \omega_r t$ = Angular spatial displacement of point P from reference point.
T = Unidirectional torque on the rotor.
k_s = Winding factor (distribution and chording) of rotor winding.
N = Rotor conductors per phase.
l = Effective rotor conductor length.
r = Radius of action of force on rotor conductors.
Z = Impedance of rotor per phase.
ϕ = Phase angle of rotor impedance.
R = Resistance of rotor.
L = Inductance of rotor.
$k = \omega_r/\omega$.
$k_0 = (\omega_r/\omega)_{T=0}$ (synchronous speed).
q = Resistance/reactance ratio.
δ = Slope of tangent to torque/speed curve at zero speed.
γ = Slope of tangent to torque/speed curve at zero torque.

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.
Mr. Connelly is with Sangamo Weston, Ltd.

(1) INTRODUCTION

A 2-phase induction motor is used in servo mechanisms to provide error-controlled motion of some member of the mechanism relative to some other member. The methods by which control of speed and direction of motion of the motor are attained are usually by supplying the first phase of the motor from a reference source of fixed voltage and frequency and supplying the second phase (*a*) from a source of fixed voltage and variable phase relative to that of the first phase; or (*b*) from a source of voltage which is in quadrature with the voltage of the first phase and which can be varied from its full normal amplitude in one direction through zero to its full normal amplitude in the opposite direction. Each change represents conversion of the motor from 2-phase to single-phase operation (and back again) and is effected by a change in voltage of the supply.

An alternative possibility is presented by a special construction of 2-phase motor in which the change from 2-phase to single-phase operation is effected by a mechanical change in the machine itself and not by a change of the supply voltage. This is illustrated in Fig. 1.

Fig. 1 shows a 2-phase machine in which the phase windings are held on two mechanically separate cylindrical stators arranged to have a common axis and to be capable of angular displacement relative to each other. The position of the outer stator is fixed by the frame of the machine and for the purpose of explanation is shown to have a magnetic axis in the vertical plane. The magnetic axis of the inner stator can be altered from the horizontal plane in one direction through the vertical plane to the horizontal plane in the opposite direction. This change is represented by a change of the angle α. When $\alpha = \pm 90°$ the condition for a rotating magnetic field is obtained; when $\alpha = 0$ a pulsating magnetic field is obtained.

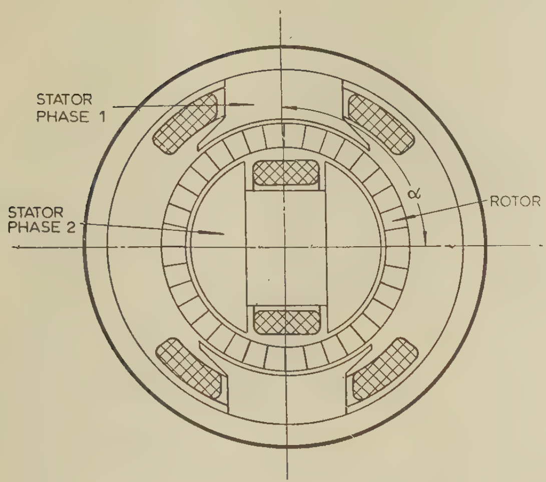
If a cup-shaped rotor is mounted to lie within the annular space between the two stators, it will be driven as for a 2-phase machine in either a positive or a negative direction when $\alpha = +90°$ or $\alpha = -90°$, and will experience the pulsating field when $\alpha = 0$ as for a single-phase machine. Thus variation of α effects a change from 2-phase to single-phase operation.

The paper deduces the relationships between the control element (voltage or mechanical displacement) and the performance of the machine under the possible conditions of variation of the former. It indicates the characteristics required in the motor to provide the service demanded of it.

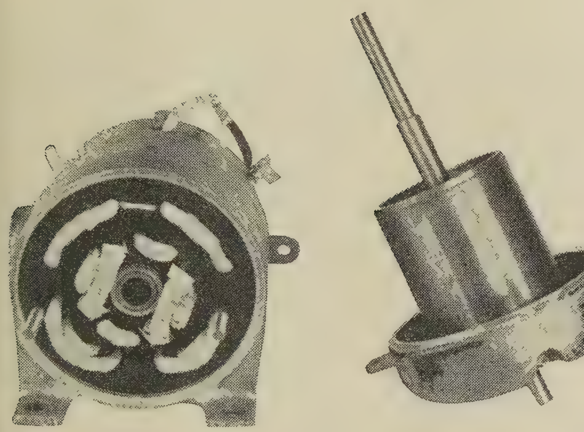
(2) THEORETICAL TREATMENT OF THE INDUCTION MOTOR

It is generally considered desirable that the flux-density distribution in the air-gap of an a.c. machine shall be sinusoidal both in space and time, and the starting-point of the analysis is the assumption that this condition has in fact been achieved. This implies that harmonics are neglected.

Further, in induction-motor theory it is customary to consider the slip in preference to the rotor speed. This practice will not be adopted here, for interest in the motor performance as a servo device lies in the speed variation about zero and not in speeds approaching the synchronous.



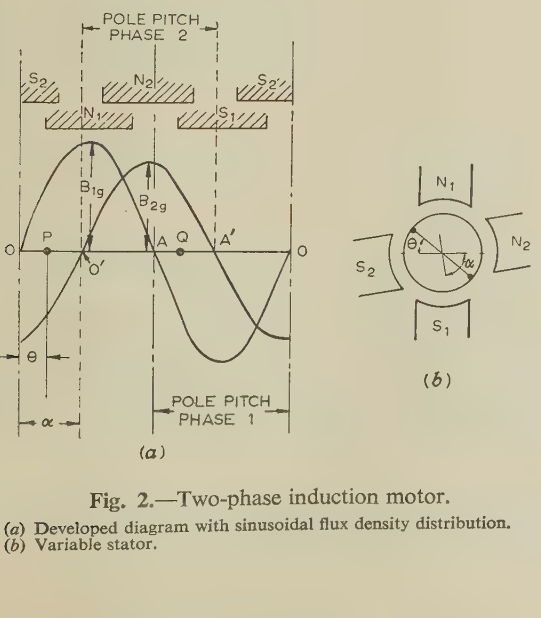
(a)



(b)

Fig. 1.—Two-stator 2-phase variable-speed induction motor.

(a) Principle of operation.
(b) Actual machine with cup-shaped rotor withdrawn from annular gap between stators.



(a) (b)

Fig. 2.—Two-phase induction motor.

(a) Developed diagram with sinusoidal flux density distribution.
(b) Variable stator.

Fig. 2 shows a 2-pole 2-phase machine. The stator windings are usually distributed over the periphery of the gap, but for convenience the pole-pairs are shown as N_1S_1 for the first phase and N_2S_2 for the second phase. The point O mid-way between N_1S_1 is chosen as reference, and the flux density at a point P due to N_1S_1 is $B_1 = B_{1g} \sin \theta$. The flux density is caused by alternating currents in the stator windings, and $B_{1g} = B_{1m} \sin \omega t$.

If the mechanical angular displacement between N_1 and N_2 is α, the time phase displacement between the flux densities due to N_1 and N_2 is ψ, and if the peak flux densities are such that $B_{2m} = aB_{1m}$, the flux density at P in Fig. 2, due to the currents in both stator windings, is

$$B = B_{1m}[\sin \omega t \sin \theta + a \sin (\omega t - \psi) \sin (\theta - \alpha)] \quad (1)$$

Integration of this expression over the angular span of the coil of which the conductor at P forms one coil side gives the total flux which interlinks with the coil.

When the rotor moves with angular velocity n through this field the angle θ becomes a function of time, i.e. $\theta = 2\pi nt = \omega_r t$, and the rotor e.m.f. obtained by differentiation of the flux with respect to time will have components of angular frequencies $2\pi(f + n)$ and $2\pi(f - n)$. The rotor circuit will present impedances

$$Z_1 = R + j2\pi(f + n)L \text{ and } Z_2 = R + j2\pi(f - n)L$$

and the rotor currents will have corresponding components in these frequencies and with corresponding phase angles $\phi_1 = \arctan 2\pi(f + n)L/R$ and $\phi_2 = \arctan 2\pi(f - n)L/R$. The value of the current in the rotor thus becomes

$$I = \tfrac{1}{2}k_s NB_{1m}lr\left\{\frac{\omega + \omega_r}{Z_1}[\cos(\omega t + \omega_r t - \phi_1) + a\cos(\omega t + \omega_r t - \psi - \alpha - \phi_1)] + \frac{\omega - \omega_r}{Z_2}[\cos(\omega t - \omega_r t - \phi_2) + a\cos(\omega t - \omega_r t - \psi + \alpha - \phi_2)]\right\} \quad (2)$$

The torque on the conductor at P (Fig. 2) is now obtained as the product of flux density, current and effective conductor length, and the resultant torque is the sum of the torques on individual conductors round the rotor periphery. The expression for this torque comprises a vibratory component and a unidirectional component, and the latter causes rotation of the rotor. It is this unidirectional torque which is of primary interest:

$$T = K\left\{\frac{Q}{2}\cos\phi_2[1 + a^2 + 2a\cos(\psi - \alpha)] - \frac{P}{2}\cos\phi_1[1 + a^2 + 2a\cos(\psi + \alpha)]\right\} \quad (3)$$

where
$$K = (\tfrac{1}{2}k_s NB_{1m}lr)^2$$
$$Q = (\omega - \omega_r)/Z_2 = \omega(1 - k)/Z_2$$
$$P = (\omega + \omega_r)/Z_1 = \omega(1 + k)/Z_1$$
$$Z_1 = \omega L[q + j(1 + k)]$$
$$Z_2 = \omega L[q + j(1 - k)]$$
$$R = q\omega L$$

Eqn. (3) gives the torque of a 2-phase induction motor in any of the conditions of use already described. From it the torque/speed relationships can be found.

(3) CONDITION OF USE OF THE TWO-PHASE SERVO MOTOR

(3.1) The Two-Phase Motor

In a normal 2-phase machine operated from a normal 2-phase supply the axes of N_1S_1 and N_2S_2 are in space quadrature and

the two flux densities have equal maxima and are in time quadrature. Thus $B_1 = B_2$, $a = 1$ and $\psi = \alpha = 90°$.

Eqn. (3) reduces to

$$T = 2KQ\cos\phi_2 = \frac{2K\omega(1-k)R}{Z_2^2}$$

$$= \frac{2K}{L}\frac{(1-k)q}{[q^2+(1-k)^2]} \quad . \quad . \quad . \quad . \quad . \quad . \quad . \quad (4)$$

This is quite easily shown to be in conformity with the expression for the torque of a normal polyphase induction motor as given in the literature, as will be seen if the term $(1 - k)$ is replaced by the slip s.

(3.2) The Single-Phase Motor

(3.2.1) Amplitude Control.

If the control of the servo motor is exercised by alteration of the amplitude of the voltage applied to the second phase, then (with the 2-phase voltages in quadrature) the motor becomes a single-phase motor when the second phase voltage reduces to zero. This condition implies that $a = 0$ and $\psi = \alpha = 90°$.

$$\text{Then } T = K\left(\frac{Q}{2}\cos\phi_2 - \frac{P}{2}\cos\phi_1\right)$$

$$= K\left[\frac{(\omega-\omega_r)}{2Z_2}\frac{R}{Z_2} - \frac{(\omega+\omega_r)}{2Z_1}\frac{R}{Z_1}\right]$$

$$= \frac{K}{2L}\left[\frac{(1-k)q}{q^2+(1-k)^2} - \frac{(1+k)q}{q^2+(1+k)^2}\right] \quad . \quad . \quad (5)$$

(3.2.2) Voltage Phase Control.

If, on the other hand, control is exercised by alteration of the phase displacement between the two stator phase voltages, the machine again becomes a single-phase machine when the two voltages are in phase with each other.

In this case $\alpha = 90°$, $\psi = 0$ and $a = 1$. Thus

$$T = K(Q\cos\phi_2 - P\cos\phi_1)$$

$$= \frac{K}{L}\left[\frac{(1-k)q}{q^2+(1-k)^2} - \frac{(1+k)q}{q^2+(1+k)^2}\right]$$

which is twice the torque obtained in the previous case. Simplification of this expression gives

$$T = \frac{2K}{L}\left[\frac{kq(1-k^2-q^2)}{(1+k^2+q^2)^2-4k^2}\right] \quad . \quad . \quad . \quad (6)$$

(3.2.3) Space Phase Control.

For the motor of special construction shown in Fig. 1, single-phase operation occurs when $\alpha = 0$, $\psi = 90°$ and $a = 1$. Since in the torque expression [eqn. (3)] ψ and α can be interchanged without affecting the result, the torque equation for this condition is the same as that of the previous Section [eqn. (6)].

(3.2.4) Torque/Speed Relations.

The relationship shown in eqn. (6) has been plotted in Fig. 3 for various values of q over a speed range $k = 0$ to $+2$. Inspection of the equation shows that over the speed range $k = 0$ to -2 the curves would be identical in shape but of opposite sign.

Fig. 3 shows that at zero speed the single-phase induction motor always provides zero torque and is thus inherently non-self-starting. When the rotor resistance/reactance ratio is small

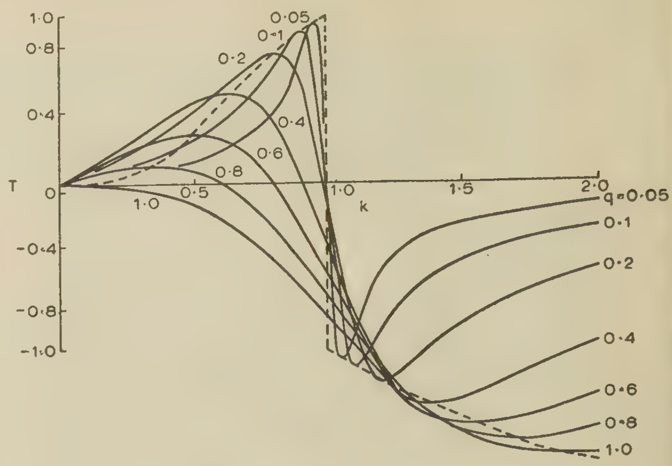


Fig. 3.—Torque/speed relations for a single-phase induction motor for various values of q.

(e.g. $q = 0{\cdot}1$) the torque rises to a maximum at a speed a little below $k = 1$ and falls rapidly to zero when k is just less than unity. As q is increased the same type of curve is obtained but it rises to a smaller maximum and falls to zero at increasingly lower speeds until, when $q = 1$, the torque is zero at zero speed and becomes negative at positive speed. A single-phase induction motor with resistance/reactance ratio of unity or greater can then never run freely; it must be driven in a positive direction against a negative torque or driven in a negative direction against a positive torque. In a servo mechanism the 2-phase motor is reversed in direction of running by first converting it into a single-phase motor and then into a polyphase motor of opposite phase sequence. If it is required to have a stable zero speed during this change-over it must be designed to have a resistance/reactance ratio equal to (or greater than) unity, i.e. $q \geqslant 1$.

The locus of the maxima of the curves for various values of q is shown by the dotted line. As q is reduced the maximum of T increases, but at the hypothetical condition $q = 0$, T collapses suddenly to zero (and reverses) at the speed $k = 1$.

A 2-phase motor operating against a constant torque runs at nearly constant speed in a given direction. A single-phase motor provides zero torque at zero speed and a positive torque at positive speed or a negative torque at negative speed. Change of operation of a machine from the 2-phase condition to a single-phase condition thus provides inherently the possibility of a change of speed. This is the important condition for operation of the servo motor and will now be considered.

(3.3) Two-Phase to Single-Phase Operation

Change from 2-phase to single-phase operation can be effected in one of three ways:

(*a*) The amplitude of the second phase voltage may be reduced from 100% to zero whilst the phase angle is maintained constant at 90° (amplitude control).

(*b*) The phase angle of the second phase voltage may be altered from quadrature to in-phase with the first phase voltage, whilst its magnitude remains constant (voltage phase control).

(*c*) The mechanical angle between the inner and outer stators of the machine of Fig. 1 can be altered from 90° (for a 2-pole machine) to zero (space phase control).

(3.3.1) Amplitude Control.

For condition (*a*) $\alpha = \psi = 90°$ and a is variable.

Then

$$T = K\left[\frac{Q}{2}\cos\phi_2(1+a)^2 - \frac{P}{2}\cos\phi_1(1-a)^2\right]$$

$$= \frac{K}{2L}\left[\frac{(1-k)q(1+a)^2}{q^2+(1-k)^2} - \frac{(1+k)q(1-a)^2}{q^2+(1+k)^2}\right]$$

which reduces to

$$T = \frac{Kq}{L}\left[\frac{k(1+a^2)(1-q^2-k^2)+2a(1+q^2-k^2)}{(1+q^2+k^2)^2-4k^2}\right] \quad (7)$$

(3.3.2) Voltage Phase Control.

For condition (*b*) $\alpha = 90°$, ψ is variable and $a = 1$. Then

$$T = K[Q\cos\phi_2(1+\sin\psi) - P\cos\phi_1(1-\sin\psi)]$$

$$= \frac{K}{L}\left[\frac{(1-k)q(1+\sin\psi)}{q^2+(1-k)^2} - \frac{(1+k)q(1-\sin\psi)}{q^2+(1+k)^2}\right]$$

which reduces to

$$T = \frac{2Kq}{L}\left[\frac{(1+q^2-k^2)\sin\psi + 2k(1-q^2-k^2)}{(1+q^2+k^2)^2-4k^2}\right] \quad (8)$$

(3.3.3) Space Phase Control.

For condition (*c*) α is variable, $\psi = 90°$ and $a = 1$.

Inspection of eqn. (3) shows that this gives the same expression for the torque as in eqn. (8) by substitution of α for ψ.

(3.3.4) Torque/Speed Relations.

Characteristic curves relating torque T and speed k for various values of the variable quantity are shown in Figs. 4 and 5. In each case the extreme values of the variable correspond to the two conditions: 2-phase and single-phase operation. The intermediate curves represent the operating characteristics during the transition condition between the two extremes. In Fig. 4(*a*) the amplitude has been altered from unity, corresponding to 2-phase operation in which the two voltages are of equal amplitude and in time quadrature, to the condition where one of the voltage amplitudes is reduced to 0·8, 0·6, 0·4 and 0·2 of that of the other voltage, giving the transition conditions. Finally the amplitude is reduced to zero, resulting in a single-phase machine.

Fig. 4(*a*) is plotted for a ratio $q = 0{\cdot}1$ and shows the small torque at zero speed increasing to a relatively large maximum just below synchronous speed as usual. Fig. 4(*b*) is for the same condition of amplitude change as Fig. 4(*a*), but is for a ratio $q = 1$. Fig. 5 shows the same kind of conditions as Fig. 4, but for these curves the variable quantity is either the phase angle ψ between the voltages applied to the two phases or the mechanical angle α between the inner and outer stator windings. When $\psi = 90°$ or when $\alpha = 90°$ a 2-phase motor results; when $\psi = 0$ or $\alpha = 0$ a single-phase motor results. Figs. 4(*a*) and 5(*a*) exhibit the same type of characteristic. So also do Figs. 4(*b*) and 5(*b*).

The point of particular interest in relation to the curves of Figs. 3, 4(*b*) and 5(*b*) is that the speed at which the torque becomes zero does not correspond to $k = 1$, which is usually understood to be the synchronous speed. In view of this fact some further consideration is now given to the question of the synchronous speed of an induction motor—polyphase and single phase.

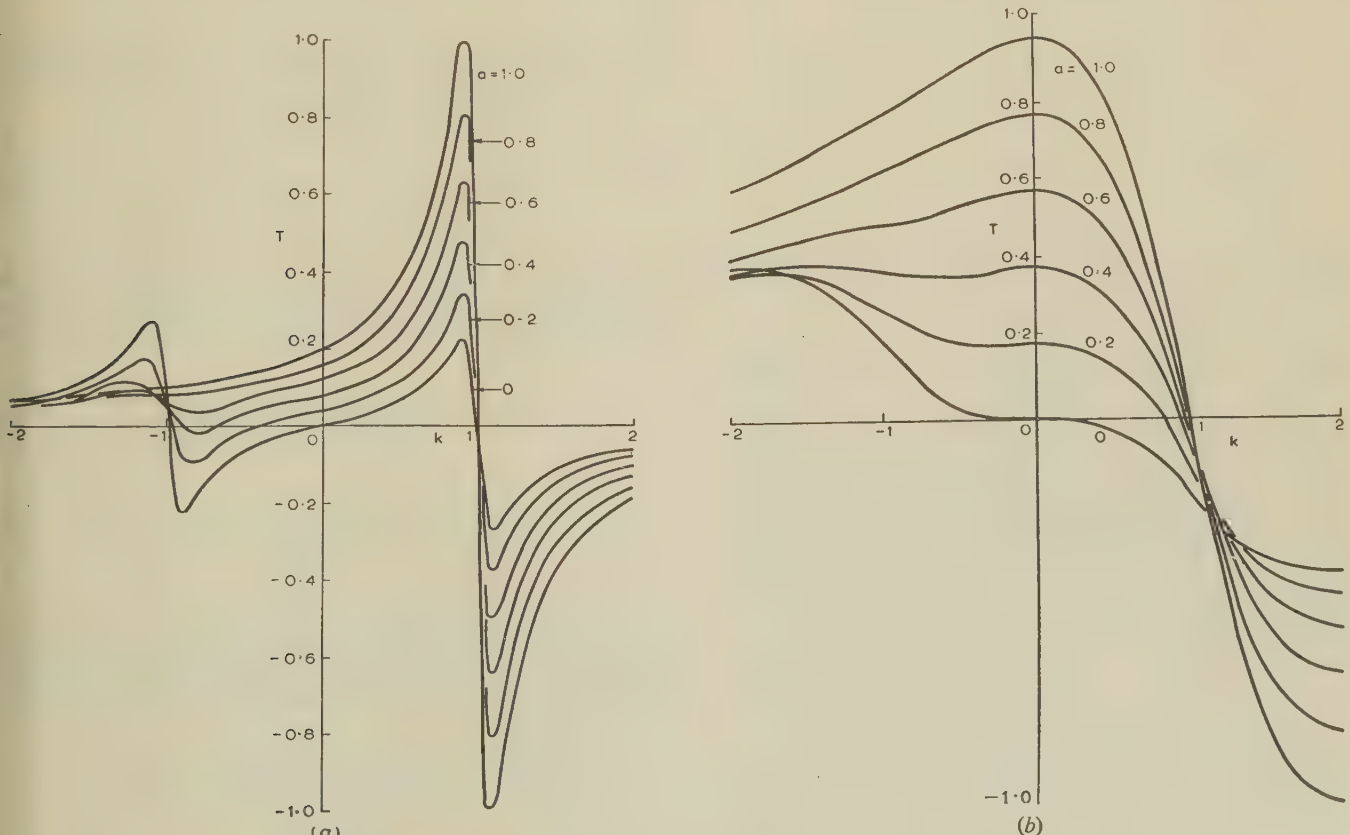
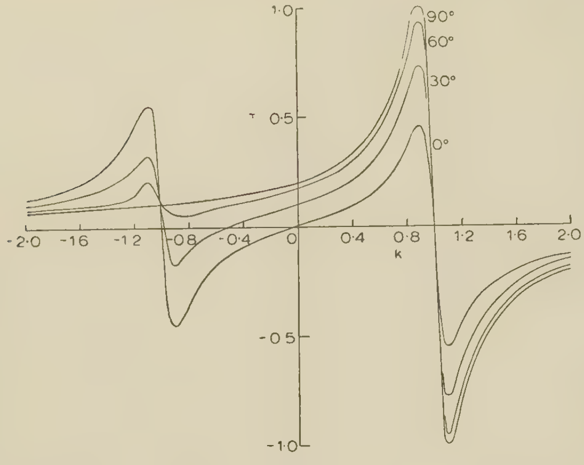
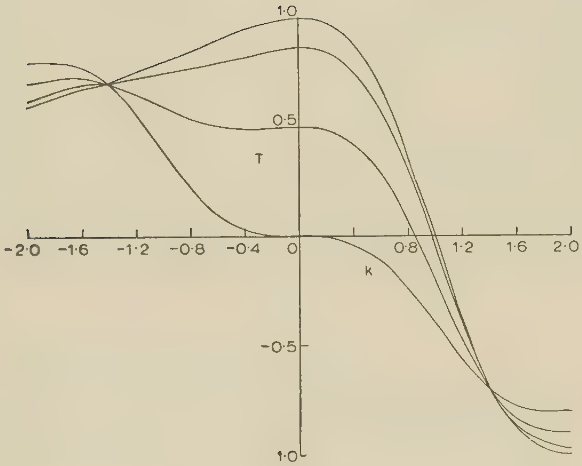


Fig. 4.—Induction motor altered from 2-phase to single-phase operation by change of amplitude of one of the phase voltages.
(*a*) $q = 0{\cdot}1$. (*b*) $q = 1{\cdot}0$.



(a)



(b)

Fig. 5.—Induction motor altered from 2-phase to single-phase operation by change of time phase or space phase of one of the phases. Figures marked are values of ψ or α.

(a) $q = 0 \cdot 1$.
(b) $q = 1 \cdot 0$.

(4) THE SYNCHRONOUS SPEED OF AN INDUCTION MOTOR

The synchronous speed of an induction motor is usually considered in relation to a polyphase machine and is defined as the speed of rotation of the magnetic field produced by the stator currents. It can be measured by determination of the rotor speed at which the e.m.f. induced in the rotor is zero. Under this condition the induced currents in the rotor are zero and consequently there is no torque on it. It will be seen that there are here two conditions which define synchronous speed, namely zero e.m.f. and/or zero torque.

In general, the e.m.f. induced in the rotor is not zero; it is only in the special case of a constant-magnitude rotating field produced by a 2-phase (or polyphase) current system that this e.m.f. reduces to zero at a certain speed—the synchronous speed. In general, also, the torque on the rotor has a unidirectional component and a pulsating or vibrating component and again it is only in the special case of a constant rotating field that both components reduce to zero, at the synchronous speed.

For a motor used as a single-phase machine, or when used intermediately between single and 2-phase, the vibratory component does not reduce to zero, but investigation of eqns. (5)–(8) shows that at some speed it is possible for the unidirectional component of torque to do so. The motor cannot, therefore, run above or even attain this speed. It is thus considered reasonable to define this as a synchronous speed, and give the general definition as *the speed at which the unidirectional torque is zero.*

On this basis the condition for synchronous speed (denoted by k_0) can be obtained by equating to zero the expression for torque given in eqns. (4)–(8).

(4.1) Two-Phase Motor

For a 2-phase motor, from eqn. (4), synchronous speed occurs when

$$\frac{(1-k_0)q}{q^2+(1-k_0)^2} = 0 \quad . \quad . \quad . \quad . \quad . \quad (9)$$

whence $\qquad k_0 = 1$

This condition is well known.

It will be noted that the synchronous speed is unaffected by the value of q.

(4.2) Single-Phase Motor

For a single-phase motor, from eqn. (6), synchronous speed occurs when

$$\frac{k_0 q(1-k_0^2-q^2)}{(1+k_0^2+q^2)^2-4k_0^2} = 0 \quad . \quad . \quad . \quad . \quad (10)$$

whence $\qquad k_0 = \pm (1-q^2)^{1/2}$

or $\qquad k_0 = 0$

The first condition shows that a single-phase motor has a synchronous speed less than that of a polyphase motor and further that it is dependent upon the ratio q of the rotor.

The second condition corresponds to zero torque at zero speed.

(4.3) Two-Phase to Single-Phase Operation

(4.3.1) Amplitude Control.

For a machine running in the intermediate condition between 2-phase and single-phase operation, by alteration of the amplitude of one phase voltage, from eqn. (7) synchronous speed occurs when

$$\frac{(1-k_0)q(1+a)^2}{q^2+(1-k_0)^2} = \frac{(1+k_0)q(1-a)^2}{q^2+(1+k_0)^2}$$

from which

$$a = \frac{(1-q^2-k_0^2)-\{(1-k_0^2)[(1+q^2+k_0^2)^2-4k_0^2]\}^{1/2}}{k_0(q^2+k_0^2-1)} \quad (11)$$

(4.3.2) Voltage Phase Control.

For a machine running in the intermediate condition between 2-phase and single-phase operation, by alteration of the phase angle between the two phase voltages, from eqn. (8) synchronous speed occurs when

$$\frac{(1-k_0)q(1+\sin\psi)}{q^2+(1-k_0)^2} = \frac{(1+k_0)q(1-\sin\psi)}{q^2+(1+k_0)^2}$$

from which

$$\sin\psi = \frac{k_0(q^2+k_0^2-1)}{q^2-k_0^2-1} \quad . \quad . \quad . \quad . \quad (12)$$

(4.3.3) **Space Phase Control.**

For the machine of Fig. 1 using alteration of the mechanical angle between the two stators, α is substituted for ψ, giving

$$\sin \alpha = \frac{k_0(q^2 + k_0^2 - 1)}{q^2 - k_0^2 + 1} \quad . \quad . \quad . \quad . \quad . \quad (13)$$

(4.4) Synchronous Speed Characteristics

From eqns. (9)–(13), curves may be drawn showing the relationships between the synchronous speed and the rotor resistance/reactance ratio for various constant values of the

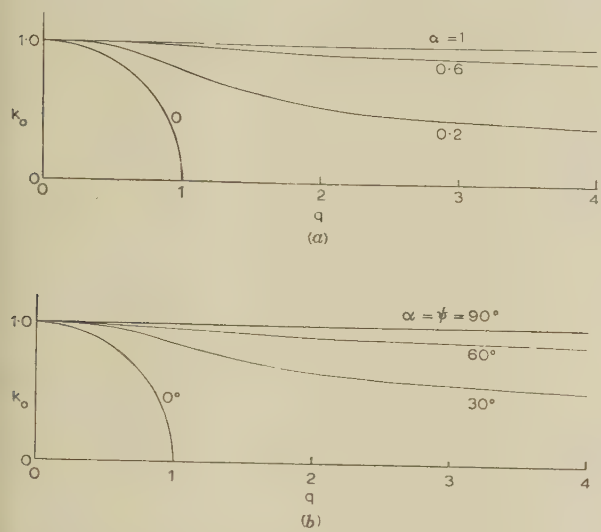


Fig. 6.—Relation between synchronous speed and rotor resistance/reactance ratio for induction motor altered from 2-phase to single-phase operation.

(*a*) By change of amplitude of one voltage.
(*b*) By change of phase of one voltage.

parameters a, ψ and α, i.e. for constant amplitude or phase displacement of one phase relative to that of the other.

(*a*) In Fig. 6(*a*), k_0 is plotted against q for $a = 1$, 0·6, 0·2 and 0. For $a = 1$, the curve is a straight line through $k_0 = 1$, showing that the synchronous speed is independent of q for a 2-phase machine. For $a = 0$ the condition for a single-phase machine is shown for which the synchronous speed is zero when $q = 1$.

(*b*) Fig. 6(*b*) shows a relationship similar to that of Fig. 6(*a*) but for relative variation of the phase displacement of the voltage or mechanical angle.

It is also possible to show the relationship between the synchronous speed and the amplitude or phase displacement for various constant values of q. The curves of Fig. 7 show that if q is low, say 0·1, the change in speed between the maximum and zero value of the variable parameter ($a = 1$ to $a = 0$, $\psi = 90°$ to $\psi = 0°$, or $\alpha = 90°$ to $\alpha = 0$) is very small. That is, the change in speed between polyphase and single-phase running of the motor is very small, which probably accounts for the fact that it is usually considered that the synchronous speed of a single-phase motor is the same as that of a polyphase motor (with the same number of pole-pairs produced by the stator currents). This is not actually true, as is shown by the curves for which $q = 1$, which fall to zero for single-phase operation (corresponding to $a = 0$ or $\psi = \alpha = 0$).

The curves show that if change of direction of rotation of the motor is required it can be achieved through a stable zero pro-

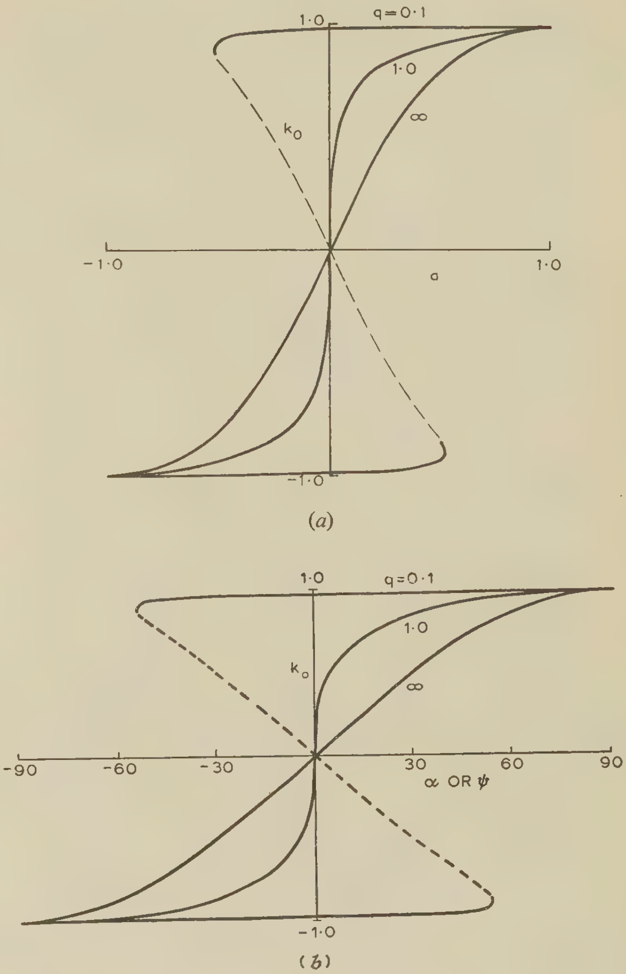


Fig. 7.—Variation of synchronous speed of a 2-phase induction motor for various values of q.

(*a*) With amplitude.
(*b*) With phase displacement.

vided $q = 1$ (or greater) but that if q is less than unity a condition of instability occurs wherein no stable zero-speed condition exists, and the motor will alter suddenly from a forward to a reverse direction. This is because the control element (a, ψ or α) must be adjusted in such a way that a negative torque is produced before the positive torque causing rotation can be cancelled, thus stopping the motor. As the motor approaches zero positive speed the negative torque exceeds the positive, causing immediate reverse running and no stable zero.

The curve for $q = 1$ shows that the positive torque is reduced to zero speed so that a stable zero is attained. A satisfactory servo motor requires a stable zero, and the curves indicate the necessary design characteristic of the motor to achieve this.

It is to be noted that a linear change of speed with change of a, ψ or α is not theoretically attainable, for even when $q = \infty$ a straight line curve does not occur.

(5) EXPERIMENTAL EVIDENCE

In order to verify the validity of the equations, comparison has been made between curves calculated from the equations and curves for the same set of conditions obtained by measurement on a 2-phase servo motor. The machine represented by the curves is a 50-volt 50 c/s 2-phase servo motor, and the curves,

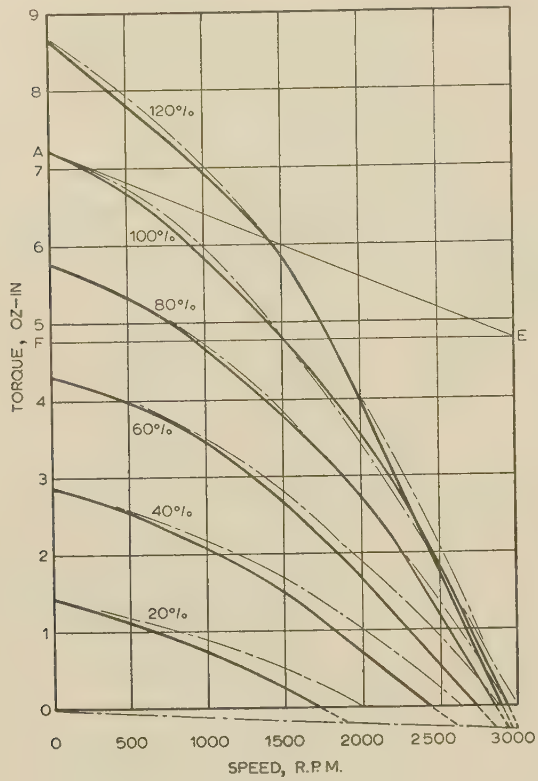


Fig. 8.—Average performance at 50 c/s of 2-phase servo motor with amplitude control, and superimposed theoretical curves.

——— Average of measured values.
----- Extrapolation.
—-—- Theoretical curves.

With 100% torque at 7·2 oz-in and 100% speed at 3000 r.p.m. slope of tangent at $T = 0$ is $\frac{AF}{AO} = \frac{2\cdot45}{7\cdot2} = 0\cdot34$

$q^2 = \frac{1 + 0\cdot34}{1 - 0\cdot34} = 2\cdot03 : q = 1\cdot425$

as supplied by the manufacturer, reproduced in Fig. 8, show the measured average performance of this type of motor.

(5.1) Procedure

To compare theoretical and practical curves it is first necessary to obtain the value of q for the motor concerned.

It is customary to determine the rotor impedance of an induction motor by the no-load (open-circuit) and locked-rotor (short-circuit) tests. Inherent in this procedure is the assumption that the stator equivalent impedance remains constant over the operating range of the machine. The machine is then represented by an equivalent T-network in which the only variable is the rotor equivalent impedance in which the equivalent resistance is a function of the slip. Accuracy in determination of the rotor impedance by this method is dependent upon the validity of the equivalent circuit and the fact that there is a considerable difference in magnitude between the no-load and locked-rotor currents taken by the machine.

With the servo motor under consideration, the construction of the machine, which has been dictated by the necessity for smooth operation at all speeds, includes skewing of the rotor conductors and an amount of mutual coupling between the two stator windings which is probably greater than that which would exist with a larger machine.

It is of interest also that tests reveal that a difference may exist between the rotor impedance referred to phase 1 of the machine and that referred to phase 2. Further, the current change from no load to full load is approximately only 60%, which is very small compared with that of a larger motor. In consequence it is doubtful whether the equivalent circuit as described above would represent the machine satisfactorily. It is likely, however, that the method used to obtain smooth operation results in an effective flux-density distribution which is nearly sinusoidal. The theory should thus be applicable to the machine, but the rotor resistance/reactance ratio must be determined from factors dependent only upon the rotor and not upon the equivalent circuit as previously. The alternative method of determination of this ratio employs the torque/speed characteristic of the machine as explained in Section 8.1. This characteristic is dependent only on those factors which have been considered in the foregoing theory, i.e. the change of flux-density distribution at the rotor and the rotor impedance.

When q has been thus determined, substitution into eqn. (7) provides theoretical curves which can be compared with experimental ones. A further check on the theory is afforded by consideration of the intercepts of the curves on the torque axis.

From eqn. (7), if the zero-speed condition $k = 0$ is substituted,

$$T_{k=0} = \frac{2Kaq}{L(1 + q^2)} \quad . \quad . \quad . \quad . \quad . \quad (14)$$

from which it is seen that the zero-speed torque is directly proportional to the flux ratio a. The relation between zero-speed torque and phase 2/phase 1 voltage ratio should then be a straight line passing through zero.

Similarly, from eqn. (8),

$$T_{k=0} = \frac{2K}{L}\,\frac{\sin\psi q}{1 + q^2} \quad . \quad . \quad . \quad . \quad . \quad (15)$$

and a similar relationship will give a sine curve.

(5.2) Results

The method of Section 8.1 has been applied to the test results of Fig. 8.

The only curve of Fig. 8 to which the method is applicable is that for 2-phase operation where equal voltages are applied to the two phases and the voltages are in quadrature. The slope of the tangent to this curve at zero speed is 0·34, whence $q = 1\cdot425$. This has been used to calculate from eqn. (7) the curves shown as dotted lines in Fig. 8.

The dotted curves for $a = 1$ and $0\cdot8$ lie close to the measured ones, but the latter are average curves, not those for one specific machine. The discrepancy in shape between the curves may be due to this fact. For $a = 0\cdot6$, $0\cdot4$ and $0\cdot2$ the theoretical curve lies above the practical curve. This can be attributed to the loss of useful torque due to windage.

The synchronous speed of the 2-phase motor, curve $a = 1$, is at $k = 1$. If this curve is extrapolated to intercept the $k = 1$ line, the intercept below the k-axis corresponds to the torque lost due to windage. At zero speed, windage is zero and friction is negligible. If a line is drawn from the origin to the intercept of the $a = 1$ curve with the $k = 1$ curve, its ordinates at any value of k can be taken to correspond approximately to the windage torque at that speed. If the curves for $a = 0\cdot8$, $0\cdot6$, $0\cdot4$ and $0\cdot2$ are extrapolated to intercept this last line, their intercepts should correspond to the synchronous (zero-torque) speeds. This, it will be observed, they nearly do.

The curves of Fig. 9 show the result of measurements taken to ascertain the relations assumed in eqns. (14) and (15). For voltage-amplitude change, theory and practice agree completely.

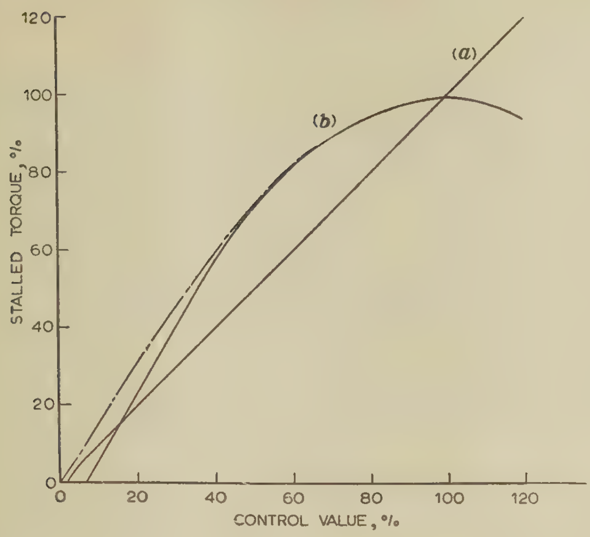


Fig. 9.—Measured and theoretical relation between per-cent stalled torque and percentage of control value, shown for size 15, 50 c/s servo motor, where 100% torque is 1·4 oz-in.

(a) Control voltage (100% = rated value).
(b) Phase angle (100% = 90°).
——— Theoretical curves.

down to about 5% amplitude. The dotted curve shows the measure of agreement obtained for phase change.

In relation to the machine of Fig. 1 the results of tests are not reproduced. It is evident from Fig. 1(*b*) that in this experimental machine insufficient copper has been used in the two stator windings. The efficiency obtained was low and the machine rapidly became overheated when on circuit. The machine was found to provide the speed control expected, but its rotor resistance/reactance ratio was high, possibly owing to the low reluctance associated with two air-gaps. Its maximum speed was lower than expected, and its actual performance is not considered to be representative of the possibilities of this type of machine. To quote its performance would therefore be misleading.

(6) CONCLUSION

The foregoing theory shows the characteristics of the unidirectional torque obtained in an induction motor. Its validity does not depend upon the manner in which the assumed flux-density distribution is actually obtained in the machine, i.e. it does not take into account the impedances of the stator windings or the physical construction of the stator. The treatment is therefore applicable to any induction device for which the initial assumption of a sinusoidal flux-density distribution can be made.

Comparisons between the theoretical relationships and measured performance characteristics on a servo motor show reasonable agreement in justification of the theory.

(7) ACKNOWLEDGMENTS

The author wishes to acknowledge his indebtedness to the Directors of Sangamo Weston, Ltd., for permission to work on the paper and to the Directors of Evershed and Vignoles, Ltd., for permission to reproduce test results on their type FZ 2-phase servo motor.

(8) APPENDIX

(8.1) Determination of the Resistance/Reactance Ratio of the Rotor

The curve AC in Fig. 10 represents the measured torque/speed characteristic of an induction motor when running as a true

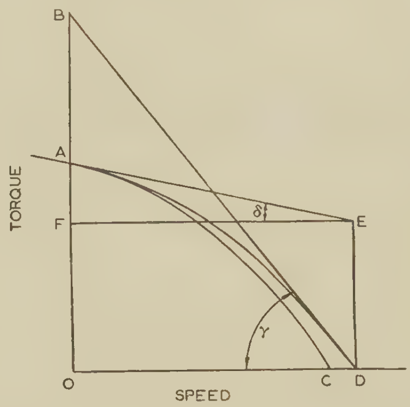


Fig. 10.—Determination of q from measured characteristic.

2-phase motor. This differs from the theoretical torque/speed relationship (shown as the curve AD) because of windage and friction. The theoretical basis of operation of the polyphase induction motor has been proved by much experiment, and it can be accepted that the curve AD is represented by eqn. (4):

$$T = \frac{2K}{L}\frac{(1-k)q}{q^2 + 1(1-k)^2}$$

If a tangent to the curve AD is drawn at the position of zero torque, its slope is

$$\tan(\pi - \gamma) = \left(\frac{dT}{dk}\right)_{k=1}$$

$$= -\frac{1}{q}\frac{2K}{L}$$

Whence $$\tan\gamma = \frac{1}{q}\frac{2K}{L}$$

Similarly, if a tangent to the curve AD is drawn at the position of zero speed,

$$\tan(\pi - \delta) = \left(\frac{dT}{dk}\right)_{k=0}$$

and $$= \frac{q(1-q^2)}{(1+q^2)^2}\frac{2K}{L}$$

whence $$\tan\delta = \frac{q(q^2-1)}{(q^2+1)^2}\frac{2K}{L}$$

From Fig. 10, $\tan\gamma = BO/OD$, and if $BO/AO = p$, where p is a numeric obtained by observation, $OD = k = 1$, and AO, the torque at zero speed, is

$$T_{k=0} = \frac{2K}{L}\frac{p}{q^2+1}$$

then $$\tan\gamma = \frac{pAO}{OD} = \frac{2K}{L}\frac{pq}{q^2+1}$$

Whence $$q^2 = \frac{1}{p-1}$$

Similarly, if $AF/AO = g$,

and $$\tan\delta = \frac{AF}{FE} = \frac{gAO}{OD}$$

then $$\tan\delta = \frac{2K}{L}\frac{gq}{(q^2+1)}$$

Whence $$q^2 = \frac{1+g}{1-g}$$

Thus q can be found from the slope of either tangent.

The measured torque/speed relationship is shown by the curve AC, which is the useful torque curve. The total torque curve AD, which is also the theoretical curve, is not directly measurable.

At zero speed, if the mechanical friction is small, as it is, the difference between the curves AC and AD is small, and the tangent to AC is practically the same as that to AD. At zero torque the speed is approaching synchronous, windage will be considerable and the two curves will diverge in such a way that the useful torque curve will have a higher slope than the total torque one. Calculation of q from the slope of the tangent drawn to the curve AC at $T = O$ will thus result in a low value of q.

It is therefore preferable to determine q from the slope of the tangent at zero speed rather than at zero torque.

DISCUSSION ON 'ENGINEERING EDUCATION AT THE TECHNICAL UNIVERSITIES IN WESTERN GERMANY'*

SHEFFIELD SUB-CENTRE, 17TH FEBRUARY, 1960

Prof. A. L. Cullen: One of the many points of interest in this paper is the concern which the teachers in the West German technical universities have about the lower 25% of their students. The absence of institutions such as our technical colleges, teaching courses equivalent to the Higher National Certificate, clearly makes matters much more difficult for them. I have heard similar views expressed by teachers in Canada and in America, where the attempt to teach students having a very wide range of abilities inevitably leads to all kinds of difficulties, including a very large failure rate at the early stages in the course. We have much to be thankful for in Great Britain, where our technical colleges are doing such an admirable job in providing the appropriate course for people who fall just short of the standard of entry at present demanded by universities.

Another important matter referred to by the authors is the 'exaggerated respect paid to "academic freedom" '. The extent to which new undergraduates differ in their ability to discipline themselves during their first year is quite remarkable. I suppose that most university teachers in this country find that it is necessary to regard the first year, at any rate for the average student, as a kind of transition between school and university. It is hardly fair to expect a man whose school teaching has been very highly organized, and who has been subjected to strict discipline in his sixth-form years, to adapt himself at once to the much freer atmosphere which ideally a university should provide. At the same time there are a number of excellent schools which provide in their sixth forms just such a transition, and in some of these the teaching is more like the ideal of university teaching than many universities are able to provide, particularly in the large first-year engineering classes.

An interesting difference between university education here and in Western Germany is the relative freedom with which students can move from one university to another in Western Germany. This seems in many ways an admirable thing and at the same time raises an apparent paradox. It seems clear from the paper that within any one technical university the co-ordination of the various courses is much less than in this country, and that amongst other things there is a good deal of overlap between various courses. On the other hand, the possibility of transfer from one university to another half-way through the course seems to imply a much bigger degree of co-ordination so far as course material is concerned between one university and another. Can the authors give any information about the way such transfers work out in practice?

I have also formed the impression from the paper that the contact between staff and students is much less close than is usually the case in this country. I would like to have the authors' comment on this remark.

Mr. D. B. Welbourn, Prof. D. B. Spalding and **Mr. G. L. Ashdown** (*in reply*): Germany possesses a large number of very fine *Ingenieurschulen* which teach exclusively to Higher National Diploma level; the difficulty of the technical universities is that they have not the legal right to exclude any would-by student who has passed *Abitur* successfully at school. Great efforts are being made to increase the number of *Ingenieurschulen* still further, and also to increase the status of the certificate which they award.

Movement between the universities is possible because they all agree to recognize the equivalence of the *Vorexamen* taken by their students. This is essentially an examination in the scientific and mathematical fundamentals of engineering, together with the essentials of engineering design. Transfers at this stage appear to work well in practice, although they are now much limited owing to the pressure on places in the universities.

Generalizations about the amount of contact between staff and students are extremely dangerous, even in this country: but probably the German student has much less contact with the staff until he has passed the *Vorexamen.*

* WELBOURN, D. B., SPALDING, D. B., and ASHDOWN, G. L.: Paper No. 2913, May, 1959 (see 106 A, p. 409).

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GAS-INSULATED POWER TRANSFORMERS

By G. CAMILLI.

(*The paper was first received* 30*th November*, 1959, *and in revised form* 20*th April*, 1960.)

SUMMARY

Power transformers have reached such a degree of perfection that it seems unlikely that there is room for substantial improvement in any fundamental characteristic. However, the insulation of conventional transformers is in the main dependent upon a liquid which is not only inflammable but has other undesirable characteristics. The substitution of gas for oil as the insulating medium eliminates the undesirable characteristics of the latter, and in addition, gas-insulated units have other advantages.

Of the various gases having desirable properties for use in modern equipment, sulphur hexafluoride is prominent. It is produced commercially in the United States.

After reviewing the advantages of gas-insulated transformers, the paper examines the fundamental characteristics of this electro-negative gas, and experimental data are presented of its electric strength relative to oil. Of considerable interest to the designer is the peculiar behaviour of gases subjected to impulse tests. It is shown that, for the same low-frequency electric strength, gas-insulated units have lower impulse strength than oil-filled transformers. This deficiency, however, can be overcome with liberal margins of safety if gas-insulated transformers are protected with modern lightning arresters.

The cooling of these new transformers is by forced circulation of the gas. The thermal capacity of gas-insulated units is, of course, somewhat lower than that of oil-filled units; theoretically, it would appear that, on this account, the overload capabilities of gas-filled transformers might be much lower than those of oil-filled units. This apparent deficiency is partially nullified by the fact that the thermal ageing characteristics of conventional class-A materials used in the construction of transformers is higher in gas than in oil. Consequently, the overload capabilities of gas-insulated units compare quite favourably with those of oil-filled units cooled by forced circulation of the liquid. Two units rated at 2000 kVA at 69 kV are in service in New York City; a third unit rated at 10000 kVA at 69 kV is in operation at Allentown, Pennsylvania. This unit is briefly described. Several other units, some of higher apparent power and voltage are now under construction.

Development is proceeding, and it appears that in the near future some very large transformers may be built which could be installed next to, or built as an integral part of, the generators, and this at considerable overall cost reduction over present practices.

(1) OIL VERSUS GAS AS AN INSULATING MEDIUM

In the very early transformers air was most commonly used as the insulating medium, but as the voltages were increased, oil was substituted for air. It took some courage to make that substitution, since oil has two objectionable characteristics which are not present in air. First, it burns when exposed to flame or heated to its ignition point in the presence of air, and secondly, certain mixtures of oil vapour and air explode on ignition when confined. Considering the large number of oil-filled transformers which have given satisfactory service and the relatively few fires, the use of oil is well justified.

However, there are cases where the use of oil is prohibited. Fire underwriters do not allow the use of oil-filled units in basements and congested areas unless they are protected by expensive fireproof vaults. For these applications dry-type transformers were developed and used. With the advent of Askarel, transformers were built using this synthetic liquid, and in many cases they were used instead of the dry type. Askarel has all the characteristics of oil and it is fire-resistant. However, it still has one of the undesirable features of oil, i.e. when an internal arc occurs, considerable pressure may be developed as the liquid decomposes. In some cases this pressure can rupture the seams of the tank.

The use of gas instead of oil completely removes the undesirable characteristics of oil. The gases used in recently developed transformers are non-flammable, and should a failure occur, the pressure would be only a fraction of that developed in a liquid-filled type. In addition, because of the compressibility and the relatively small mass of the insulating medium to be displaced during an arc, the operation of a pressure-relief diaphragm is much more reliable in a gas-insulated unit.

(1.1) Other Advantages of Gas-Insulated Units

Since they contain only a small fraction of the weight of oil (sulphur hexafluoride gas at 15 lb/in^2 is 70 times lighter than oil), gas-filled transformers are much lighter; reductions of the order of 20–30% are possible.

Since gas transmits less vibration than oil, it results in a transformer with better noise characteristics. For instance, in a 10000 kVA unit which is installed at Allentown, Pennsylvania, the sound generated by the core (exclusive of the fans) is approximately 9 dB less than the values recommended by the American National Electric Manufacturers Association for an equivalent self-cooled unit of the same rating. The noise emanating by the combined action of the core and fans is approximately 8 dB less than that of the equivalent fan-cooled unit of the same rating.

In an installation involving exclusively gas-insulated units, all the expenses associated with oil-filled apparatus can be eliminated. This involves storage tanks, pumps, filter presses, etc. Gas-filled transformers are completely sealed and the gas should never be reconditioned or replaced. Should it be deemed advisable to empty the tank, the gas can be saved and pumped back in the standard pressure cylinders.

Because of the possibility that some serious damage or accident can cause all the oil to run out of a transformer, it is quite common practice to build drainage pits and ditches around large transformer installations. This expense is, of course, unnecessary for gas-filled units. In gas-insulated units the fire hazards do not exist and the expense of possible fire walls, vaults and fog-spray equipment can be eliminated.

An item of major expense in many generating stations is the long isolated phase busbar run usually required between the generator and the transformer. In addition to the first cost, this means costly power loss. Because oil-filled transformers must be installed out of doors and often at a distance from the generator building, very little can be done to reduce this expense. However, the use of gas presents the possibility of locating the transformer flush against the outside wall of the building or even inside it, thus eliminating most, if not all, of the busbar run.

It is believed that gas transformers offer definite advantages to the industry, and their potential will be realized by the combined efforts of users and manufacturers.

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.

Mr. Camilli is with the General Electric Co., Pittsfield, Massachusetts, U.S.A.

(2) CHARACTERISTICS OF GASEOUS DIELECTRICS

The early work of Charlton and Cooper, in 1937, and Pollock and Cooper, in 1939, demonstrated that certain fluorocarbons and sulphur hexafluoride were distinctly superior to air in terms of their electric strength. Sulphur hexafluoride became commercially available in 1947. The best gaseous dielectrics are characterized by fairly heavy and complex molecules of a fluorine compound. While the chemistry of fluorine is relatively new, the number of gases which have been evaluated is very large. However, not all these gases, even if they were available, are suitable for transformer use. While electric strength is a factor of great importance in the selection of the gas, there are others to consider. The more important are boiling point (or condensation temperature), vapour pressure, toxicity, chemical inertness, thermal stability and price.[1]

(2.1) Simplified Mechanism of Breakdown

The mechanism of voltage breakdown in gases in a uniform field is believed to begin with the acceleration of a single electron starting at the cathode. This electron is accelerated toward the anode by the electric field to a velocity sufficient to ionize neutral molecules with which it collides. The secondary electrons ionize more molecules, thus exponentially building up an avalanche of many electrons as they progress toward the anode.

Sulphur hexafluoride is one of the electro-negative, or attaching, gases so called because of their ability to seize an electron and thus form a negative ion. This property makes important differences to the mechanism of gas breakdown—it can be visualized if the process of attachment is considered as the opposite of ionization. Where the ionization process is trying to release, the attachment process endeavours to remove, free electrons. In the limit, if the attachment were carried on at a rate equal to ionization, no breakdown would be possible by electronic ionization.

In a purely qualitative sense this property of the electro-negative gases can be described as that of a sponge which soaks up free electrons coming in contact with it, with the resultant choking of extensive individual streamers. The character of the gas molecule determines both the ionization and the electron attachment. Large molecules are obviously more easily hit and will cause more collisions, which reduce the average electron velocity. Thus a gas of higher molecular weight would ionize at higher voltage and will exhibit a greater electron attachment than one of lower molecular weight. This characteristic is shown in Fig. 1. As shown in the curves, the relationship applies in both uniform and non-uniform fields.

The electron attachment is correlated with the electron affinity of its constituent atoms. Atomic electron affinities are higher for atoms in the upper right-hand corner of the periodic table, which includes oxygen, sulphur, and the halogen atoms, fluorine, chlorine, bromine and iodine. With the halogen atoms the outer electron shell is most nearly complete (lacking only one electron); by acquiring one electron, and thus completing the orbital requirements, they form the most stable negative ions. Of the four halogen atoms, fluorine has the greatest electron affinity as the attachment takes place in a shell with the lowest quantum number. Stated another way, the completed valence electron shell is nearest the nucleus in the fluorine ion.

Since sulphur hexafluoride and its reaction products are strongly electron-consuming, the occurrence of an extensive space charge to weaken the gap is undoubtedly more limited than would be the case, for example, in nitrogen.

(2.2) Electric Strength and Temperature of Condensation

Table 1 lists a family of fluorine compounds. At room temperature and atmospheric pressure some of these materials

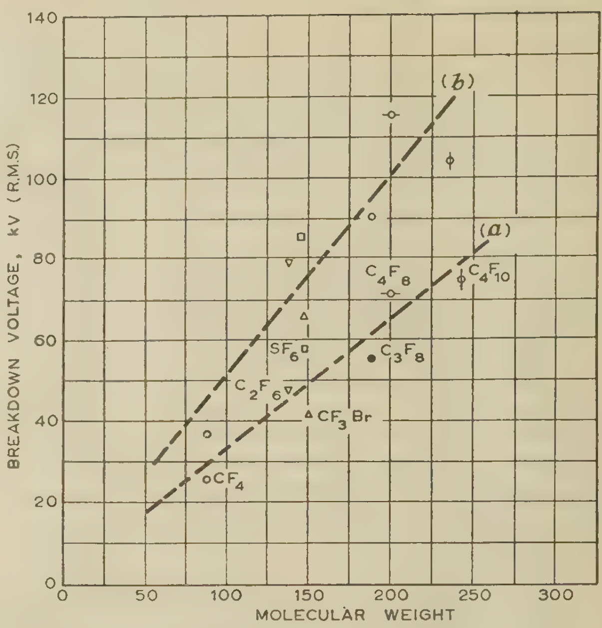

Fig. 1.—Correlation between electric strength and molecular weight of fluorogases.

(a) Tests between 3 in-diameter round-edged planes, spaced 0·5 in apart.
(b) Tests between ½ in-square rod to 3 in-diameter plane spaced 1 in apart.

are gaseous while others are liquid. It will be noted that the electric strength is related to molecular weight and condensation temperature. Thus sulphur-hexafluoride with a boiling point (or temperature of condensation) of −63·8°C has a relative electric strength 2·5 times that of nitrogen, while perfluorodimethylcyclohexane with a boiling point of 101°C has a relative strength 8·5 times that of nitrogen.

Condensation is a matter of serious concern if the equipment containing a rather high boiling-point gas must operate out of doors in a cold climate and if the electric strength of the unit depends upon all the gas being in the vaporized state. Power transformers pressurized with sulphur hexafluoride gas at, say, 15 lb/in^2 (gauge), 25°C, will retain the full electric strength even though it is energized, say, when the gas temperature is −60°C.

(2.3) Electric Strength in a Sealed System

In a closed system of constant volume the electric strength of the gas is solely determined and fixed by the initial filling.

The sets of curves shown in Fig. 2 show the variation of pressure at constant volume for several different initial fillings. Thus, if a given transformer tank is filled, say at 10 lb/in^2 (gauge) at 60°F, the pressure at 160°F will be 15 lb/in^2 (gauge), while at −40°F the pressure will be 5 lb/in^2 (gauge). Throughout this change in the pressure, if the vessel is tight, the electric strength will not change because the density of the gas is not changed.

(3) TOXICITY OF SULPHUR HEXAFLUORIDE

The toxicity of sulphur hexafluoride is a subject which has been carefully studied because gases in general present greater hazards than any other media, the reason being that one may choose whether to swallow or come in contact with a solid or liquid but no such alternative is really available for a gas.

Sulphur hexafluoride is colourless and odourless at ordinary temperatures. Tests made at the Laboratory of Applied Physiology at Yale University[2,3] show that it is physiologically inert. But in common with other inert gases, it is a simple asphyxiant. In mixtures containing 80% of sulphur hexafluoride together with 20% of oxygen it causes no toxic action, with the

Table 1

Physical Properties and Breakdown Voltage of Compounds Investigated

Compound	Molecular formula	Molecular weight	Liquid density at 20° C	Refractive index at 20° C	Boiling point	Vapour pressure at 25° C	Relative 60 c/s breakdown at 30 cm Hg
					deg C	mm Hg	
Nitrogen	N_2	28	0·808 at −196° C		−196	Gaseous	1
Perfluormethane	CF_4	88	1·96 at −184° C gas 0·60		−128	Gaseous	1·25
Perfluoropropane	C_3F_8	188	1·45 gas 0·61		−38	Gaseous	2
Sulphur-hexafluoride	SF_6	146	1·91		−63·8	Gaseous	2·5
Perfluoroheptane	C_7F_{16}	388	1·733	1·2618	82	110	5·5
Perfluorotriethylamine	NC_6F_{15}	371	1·749	1·262 at 25° C	71	130	6
Perfluoromethylcyclohexane	C_7F_{14}	350	1·7994	1·2815	76	110	6
Perfluorotoluene	C_7F_8	236	1·660	1·3664	102	23	7
Perfluorodibutyl ether	$C_8F_{18}0$	454	1·717 at 25° C	1·26	100	33	7·5
Perfluorotri-*n*-butylamine	$NC_{12}F_{27}$	671	1·88	1·29 at 25° C	178	2·7	8
Perfluorodimethylcyclohexane	C_8F_{16}	400	1·8503	1·2896	101	40	8·5
Perfluorophenanthrane	$C_{14}F_{24}$	624	2·019	1·3315	205	—	10

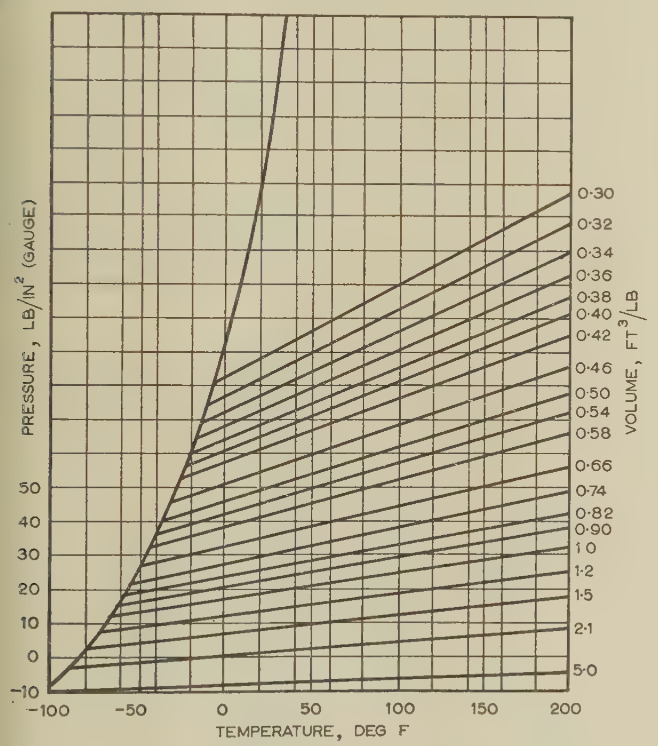

Fig. 2.—Pressure/temperature variation of sulphur hexafluoride at constant specific volume.

sole qualification that when breathed it causes changes in the pitch and timbre of the voice, as is expected because of its higher density than nitrogen. It has been used for many years in X-ray transformers which are used for medical therapy and industrial radiography.[4] The 1 MV transformer contains approximately 50 lb and the 2 MV unit approximately 250 lb of gas under pressure. Although these transformers have been installed in hospitals and industrial plants in the midst of large numbers of people, no complaint of any sort has ever been made on the health hazard.

When these units are serviced the gas is allowed to escape into the room, and it causes no problem. Although much heavier than air, it mixes very rapidly with it, and the oxygen content of the mixture is not considerably reduced. While the removal of the gas from a large transformer will be discussed later, let us assume that all the gas from, say, a 15 000 kVA gas transformer is allowed to leak into a 8 000 ft³ room. Assuming that the transformer holds 1 000 lb of sulphur-hexafluoride gas at 15 lb/in² (gauge), corresponding to a volume of 2 600 ft³ at atmospheric pressure, the amount of air left in the room after all the sulphur hexafluoride is released into it is 5 400 ft³ with an oxygen content of 1 800 ft³. The amount of oxygen, based on the total volume of the room, is reduced from 20% to 13·5%, which is equivalent to the oxygen content available in the atmosphere around a plane flying at a height of two miles.

The production of these gases involves the use of elemental fluorine, and thus necessitates a close watch being kept upon the purification process. In the synthesis of sulphur hexafluoride, the lower fluorides of sulphur are also produced, and while the hexafluoride is innocuous, the lower fluorides are very poisonous. Fortunately, extreme care is observed in the production of this particular gas for industrial use. To verify the purity of the gas, a sample of every batch, after its manufacture, is tested for toxic residuals by exposing rats to a concentration of 80% sulphur hexafluoride and 20% oxygen for a period of 14–18 hours after which the animals are examined for lung irritations and other functional injuries.

The products of decomposition formed when sulphur hexafluoride is arced have been fully covered in Reference 5. Under the action of an arc, breakdown products are formed, their nature and amount depending on the character of the energy source employed, the type of solid insulation which is used in the construction of the unit, and the amount of water vapour, air and other gases present.

The breakdown products are themselves gases usually of an acid nature, or possessing oxidizing characteristics. In contrast with a liquid dielectric, breakdown does not result in an appreciable rise in pressure. The acid and oxidizing substances may be absorbed and their health hazards nullified either by keeping them totally enclosed or by passing the arced gas through an absorbent such as aluminium oxide or soda lime.[6]

(4) COMPARISON OF THE ELECTRIC STRENGTH OF SULPHUR HEXAFLUORIDE WITH OIL

The relative electric strengths are dependent on the electrode configuration, spacings, and pressure of the gas. In a uniform field the electric strength of sulphur hexafluoride is directly proportional to pressure. The pressure-dependence characteristic of uniform fields does not hold for non-uniform ones. In some electrode configurations the strength of the electronegative gases actually decreases with pressure. This peculiar behaviour and the relative electric strength with 10C oil is shown in Figs. 3 and 4. Fig. 4 shows the relative electric strength of

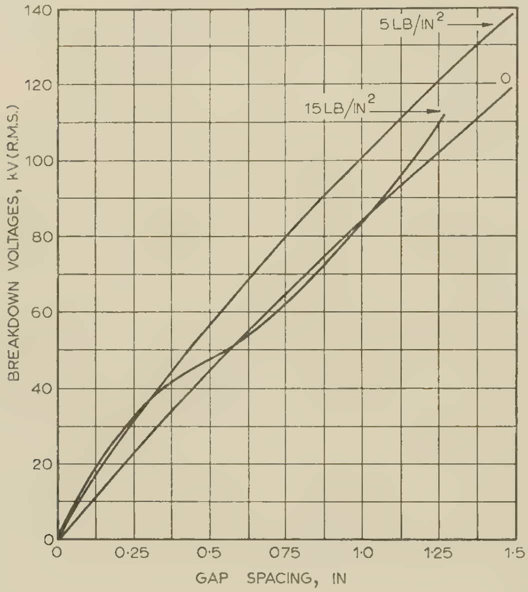

Fig. 3.—60 c/s breakdown voltages of ¼ in-square rod and plane in sulphur hexafluoride at various pressures.

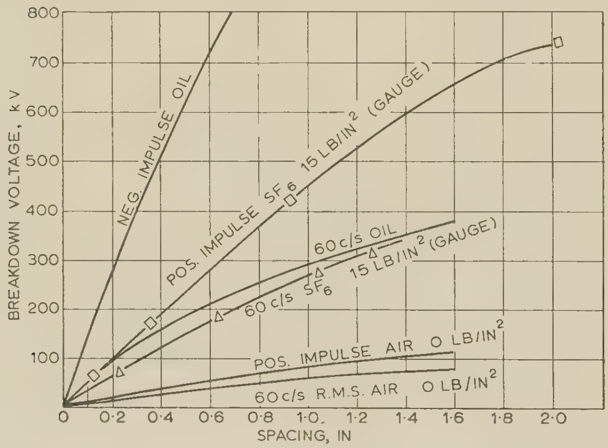

Fig. 4.—Impulse and 60 c/s electric strength of sulphur hexafluoride, air and transformer oil.

Tests between 10 in sphere to plane.

oil and sulphur hexafluoride at 15 lb/in² (gauge) in a relatively uniform field (10 in sphere to plane) at both 60 c/s and impulse, while Fig. 5 shows the relative breakdown values of oil and sulphur hexafluoride in a non-uniform field.

At present sulphur hexafluoride at 10 lb/in² (gauge) does not compare very favourably with 10C oil. It appears that the electric strength of oil can be reached when the gas is operated at 30 lb/in² (gauge) if the electric fields are fairly uniform.

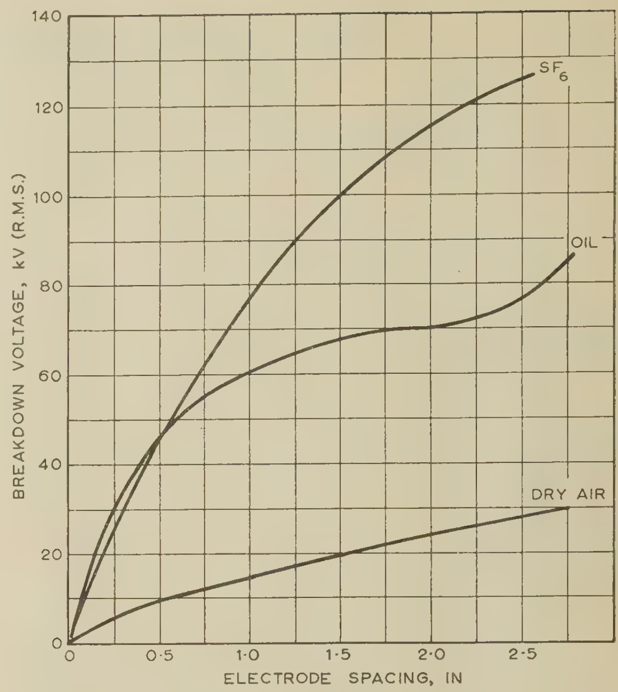

Fig. 5.—Relative electric strength of air, transformer oil and sulphur hexafluoride in a non-uniform field.

Tests made at atmospheric pressure: square-rod and plane electrodes.

Electric fields when non-uniform produce large differences between the voltages at which corona starts and those at which breakdown occurs, and in the case of impulse-voltage differences, they frequently depend on the polarity of the electrodes.

Table 2 shows the large difference in the voltages at which

Table 2

Corona onset and Flashover Voltage in Sulphur Hexafluoride—¾ in-Diameter Square-Cut Rod and Plane Electrodes. Full-Wave Tests 1½ × 40 microsec

Pressure	Spacing	Corona onset voltage		Flashover voltage	
		Point negative	Point positive	Point negative	Point positive
lb/in²	in	%	%	%	%
0	1	92	100	200	110
15	1	125	128	254	145
0	6	85	100	324	162
15	6	132	140	354	147

For each spacing, the corona onset voltage at atmospheric pressure and positive point is 100%.

corona and breakdown occur during impulse test (1·5 × 40 microsec) between ¾ in-diameter square-cut rod and plane electrodes.

Corona occurs for the negative electrodes at lower voltages than for the positive electrodes. However, the breakdown voltages are much higher than those obtained when the point is negative because the space-charge formation at the negative rod is effective in shielding the electrode. After the corona is formed the electrode configuration is in effect changed, the field is improved and the voltage at which breakdown occurs is raised considerably.

The differentials between the voltage at which corona is formed and the actual breakdown values show that the rating of a given insulation structure should not be based solely on

reakdown values if the designs comprise non-uniform fields. able 2 also shows that while the difference between the corona nset voltages at the two polarities is not considerable, the npulse flashover voltages with the rod negative are more than ouble those with the rod positive.

5) IMPULSE RATIO: COMPARISON BETWEEN GAS AND OIL STRUCTURES

From the preceding Sections it appears that, to obtain the ighest values for the corona starting voltage, gaseous insulation tructures need to be designed with essentially uniform fields. rom the practical standpoint, some form of solid insulation is ised in building up the insulation structure. Even in a well-alanced gaseous insulating design the impulse ratio is lower han that obtained from an equivalent oil-filled structure. In a as-insulating structure this ratio, which is defined as the ratio f the breakdown value at the full wave ($1\frac{1}{2} \times 40$ microsec) o the peak value of 60 c/s breakdown, ranges between 1·2 and ·4, compared with the higher values found in oil-insulated ransformers. This means that, for a given 60 c/s high-voltage est, the breakdown values at the full-wave impulse test are ower than those in an oil structure. Conversely, in a gas-nsulated structure for a given impulse level, the 60 c/s, or low-requency, withstand level is higher than in an oil-insulated unit.

The fact that for a given impulse level the low-frequency trength of a gas-insulated unit is higher than that obtainable rom an oil-insulated unit makes it relatively easy to obtain o-ordination with the modern station-type lightning arresters.[7] The comparative voltage/time characteristics of an oil structure of a typical insulating assembly comprising gas and solid insulation s shown in Fig. 6. If the characteristic of a modern lightning arrester is added to a gas-insulated transformer it will be seen (see Fig. 7) that a good margin of protection may be obtained.

Recently, we have proposed to the industry that the basic impulse level (b.i.l.) for gas-insulated units protected by modern lightning arresters should be set at one step down from the b.i.l. corresponding to the equivalent low-frequency tests. The low-frequency values should be the same as those now used in oil-insulated transformers. In an oil-filled unit, the b.i.l. is determined by the relationship

$$40\,\text{kV} + 2{\cdot}2V \qquad (1)$$

where $V = 60$ c/s dielectric test voltage.

In the case of gas-insulated units, the b.i.l. can safely be set at one step below that determined by eqn. (1). For instance, in a 69 kV oil-insulated unit it is 350 kV and the 1 min 60 c/s dielectric test voltage is 140 kV. In a corresponding gas-insulated unit protected by lightning arresters the 60 c/s test voltage is kept at 140 kV while the impulse test is 250 kV. As shown in Fig. 7, relatively large protection levels are thus obtained.

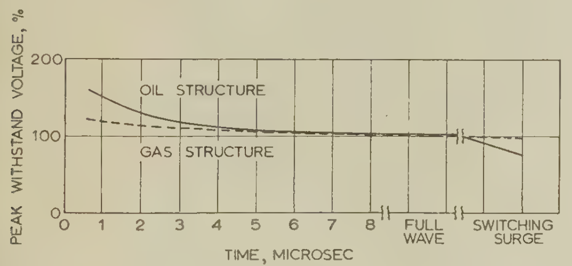

Fig. 6.—Voltage/time characteristics of oil- and gas-insulated structures.

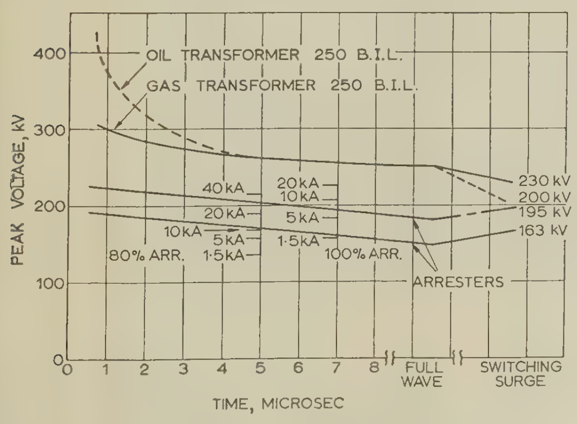

Fig. 7.—Co-ordination and protection of 69 kV oil- and gas-insulated transformers, both types rated 250 b.i.l.

(6) THERMAL CHARACTERISTICS

In the cooling of large power transformers, the gas absorbs the heat by circulation through the core and coils, and the heat is then removed to the external air by a heat exchanger.

The high specific weight of sulphur hexafluoride is mainly responsible for its superiority over air. For instance, when the gas is forced through the transformer, it absorbs heat, and the increase in temperature depends upon the product of specific weight (density) and the specific heat of the gas; while the specific heat of sulphur hexafluoride is lower than that of air, its density is several times greater. Therefore, from the aspect of heat absorption sulphur hexafluoride at atmospheric pressure is three times better than air. Thus, for a certain volume flow of sulphur hexafluoride through the heated core and coils, the temperature rise will be one-third of that obtainable if air were used.

If a gas is forced through a coil dissipating P watts/in^2, the temperature rise $(T_1 - T_2)$ of the coil over the incoming gas is determined by the relation

$$T_1 - T_2 = \frac{P}{k(pv)^n}$$

where $T_1 - T_2$ = Temperature differential of the coil over incoming gas, deg C.
p = Gas pressure, lb/in^2 (absolute).
v = Velocity of the gas, ft/min.
k = Constant, depending on the units, the characteristics of the gas and the configuration of the coil.

It is evident that the temperature rise can be controlled by the pressure and the velocity of the gas over the coil (mass transfer). Since the power required for the flow of the gas is proportional to v^3, it follows that the velocity of the gas cannot be increased to extremely high values.

(7) NOVEL METHOD OF COOLING

As stated previously, cooling is obtained by forced circulation of the gas through the core and coils as shown schematically in Fig. 8, two independent blowers being used. The motors for their operation are enclosed in separate chambers which are also independent of the main tank, so that the motors and the auxiliary bearings can be inspected without loss of gas from the transformer.

A 2-stage evaporative cooler has been developed and is used for cooling the gas. In Fig. 8, the gas, after being heated by passage through the core and coils, flows over an evaporator A which is located in the main transformer tank. The evaporator is connected to a condenser B which is located externally to the unit. The system contains a Freon liquid and when the transformer is in operation, the heated sulphur-hexafluoride gas produces evaporation of the Freon in the

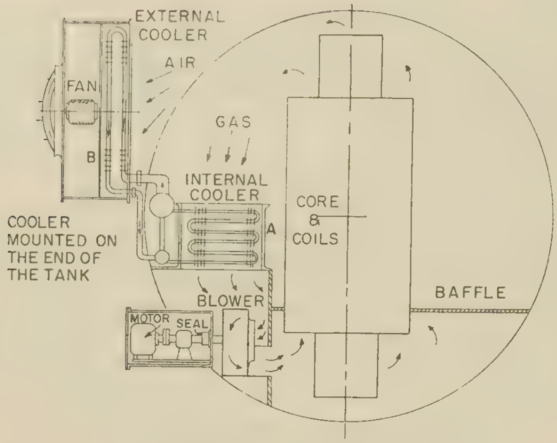

Fig. 8.—Two-stage evaporative cooling system used for the cooling of the gas.

evaporator. The vapour rises into the condenser, where it liquefies under the action of an external fan and then returns to the evaporator. The circulation is accomplished by gravity without the action of any pump. When water is available the condenser can be water cooled.

There are many advantages in the use of this system:

(*a*) The condenser can be located in any convenient place, even remote from the transformer. When the transformer is located inside a building, the condenser can be placed outside, where the ambient temperature is lower.

(*b*) The external part of the 2-stage cooler can be shipped separately from the transformer without loss of pressure from the main tank.

(8) AGEING OF MATERIALS OPERATING IN SULPHUR HEXAFLUORIDE

Controlled temperature-accelerated ageing tests employing methods based on the chemical reaction theory indicate that class-A material can be operated in an atmosphere of sulphur hexafluoride at somewhat higher temperatures than in 10C oil. Moisture seems to be the major source of degradation, and great care should be exercised in maintaining a low moisture content in this type of apparatus.

(9) OVERLOAD CAPACITIES OF GAS-INSULATED TRANSFORMERS

Gas-insulated units have an inherently shorter time-constant than equivalent oil-filled units, and therefore they will reach the ultimate hot-spot temperature faster than corresponding liquid-filled transformers. The overload capabilities of gas-insulated units can be determined by using methods similar to those already established for oil-filled units. The general method is described in the A.S.A. Guide for Loading Distribution and Power Transformers, Appendix C57.92. In the calculation of the time-constant, the thermal capacity is determined by substituting the weight of the gas and its specific thermal capacity for the equivalent values of the oil. Thus the thermal capacity C of the material used in a gas transformer (in watt-hours per pound per deg C) is

$$C = 0\cdot06 \times \text{weight (in pounds) of the core and coils} + 0\cdot06 \times \text{weight (in pounds) of the tank and cooler} + 0\cdot093 \times \text{weight (in pounds) of the gas.}$$

In a typical 10 000 kVA gas-insulated unit the time-constant is approximately 1·2 hours; while in a corresponding forced-oil air-cooled unit it is of the order of 1·5 hours. Taking advantage of the improved ageing characteristics of type A material in sulphur hexafluoride, the short-time overload characteristic of a typical 10 000 kVA unit compares quite favourably with that of a forced-oil-cooled unit of the same rating. This comparison is shown in Table 3, which applies to two units, both having a 55° C rise at rated apparent power and a ratio of I^2R to core loss of approximately 2·6.

(10) HANDLING OF SULPHUR HEXAFLUORIDE

Sulphur hexafluoride is obtained in 100 lb bottles. From the vapour-pressure characteristic shown in Fig. 9, it may be seen that at room temperature (25° C) the pressure in the bottle is approximately 355 lb/in² (absolute). In filling a transformer the tank is first evacuated and then filled at the desired pressure by connecting the gas bottle to the tank.

If, for any reason, the sulphur hexafluoride needs to be removed from the tank, it can be stored in the original shipping bottle by transferring it by means of a compressor.

Table 3

COMPARISON BETWEEN SHORT-TIME LOADING OF A TYPICAL GAS-INSULATED TRANSFORMER AND FORCED-OIL AIR-COOLED TRANSFORMER BOTH RATED 10 000 kVA

Following load	Time	Times rated kVA to use not more than following life							
		Gas unit				Oil unit			
		% 0·1	% 0·25	% 0·50	% 1·0	% 0·1	% 0·25	% 0·50	% 1·0
%	hours								
100	½	1·70	1·70	1·70	1·70	1·67	1·82	1·94	2·00
	1	1·62	1·62	1·62	1·62	1·47	1·60	1·71	1·81
	2	1·48	1·55	1·55	1·55	1·29	1·41	1·50	1·58
	4	1·37	1·47	1·52	1·52	1·18	1·28	1·35	1·43
	8	1·30	1·40	1·47	1·51	1·10	1·18	1·26	1·33
	24	1·19	1·28	1·35	1·42	1·05	1·09	1·15	1·21
50	½	1·80	1·80	1·80	1·80	1·78	1·92	2·00	2·00
	1	1·68	1·68	1·68	1·68	1·53	1·64	1·73	1·82
	2	1·50	1·58	1·58	1·58	1·32	1·42	1·49	1·57
	4	1·38	1·47	1·52	1·52	1·18	1·26	1·33	1·40
	8	1·30	1·40	1·47	1·51	1·10	1·17	1·24	1·31
	24	1·19	1·28	1·35	1·42	1·05	1·08	1·14	1·20

Class A material, 55° C average winding rise, ratio of I^2R to core loss = 2·6.

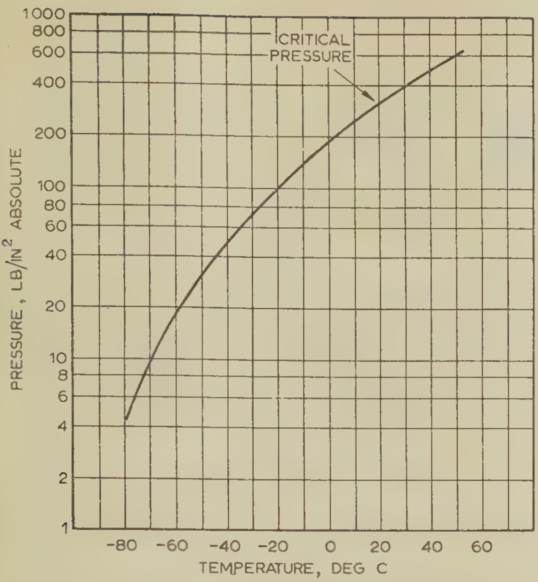

Fig. 9.—Vapour pressure of sulphur hexafluoride.

(11) DESCRIPTION OF THE 10 000 kVA GAS-INSULATED UNIT (REFERENCE 8)

The transformer is rated 10 000 kVA, 60 c/s, 62 700–12 470/7 200 volts. As shown in Fig. 10, the external appearance of the unit is somewhat unusual and does not resemble that of a conventional oil-filled unit. The reason is that, with the gas under pressure, a cylindrical tank is ideal as a pressure vessel.

minor modification to the insulating structure, the core and coils are of the conventional type, and similar to those used in oil-filled units. Similarly, class-A insulating materials have been used for the conductors and the supporting members of the core and coils. A 2-stage evaporative cooler shown in Fig. 8 is used for cooling the gas. The high-voltage windings designed and tested at 250 b.i.l. have been protected with lightning arresters connected directly to the high-voltage bushings. The low-voltage windings are not protected with lightning arresters and have been designed and tested with corresponding full b.i.l. at 110 kV.

Full low-frequency tests were applied to both high-voltage and low-voltage windings. The bushings for this unit are of the solid type with gas between the core of the bushing and the porcelain shell. These bushings are otherwise the same as the conventional oil-filled bushings. The relative weights and other characteristics of this unit are compared with an oil-filled unit of the same rating in Table 4. Relative size and floor space are given in Fig. 11.

Table 4

Comparison with Oil-Filled Self-Cooled Unit of the Same Rating

Item	Oil-filled self-cooled unit	Gas-insulated unit
Weight of core and coils ..	88%	100%
Weight of insulating medium..	2 200%	100%
Total weight of units	140%	100%
Reactance	7·2%	7·2%
Total loss	95%	100%

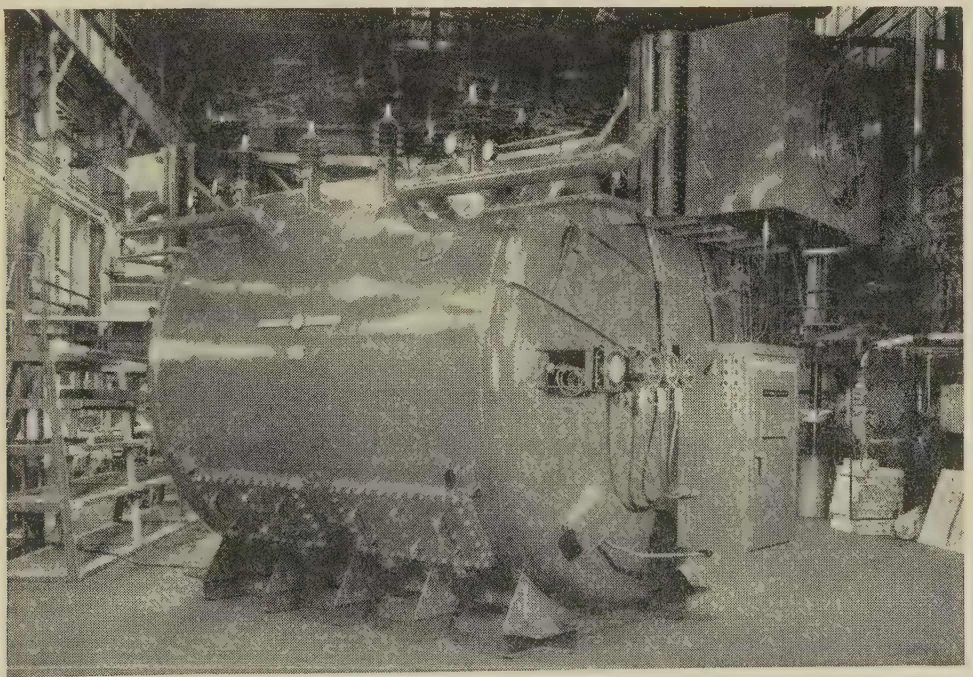

Fig. 10.—10 000 kVA, 69 000–12 470-volt gas-insulated (sulphur hexafluoride) transformers.

With the exception of the manholes, an all-welded construction is used. The tank has been designed and tested to meet the American Society of Mechanical Engineers pressure-vessel codes.

The tank is filled with sulphur hexafluoride at a nominal pressure of 15 lb/in^2 (gauge) at 25° C. With the exception of

(12) CONCLUSIONS

The 10 000 kVA gas-insulated transformer described in Section 11 is the third unit of its type to be put into service. Several additional units, some of higher apparent power and voltage, are under construction. Although much remains to be

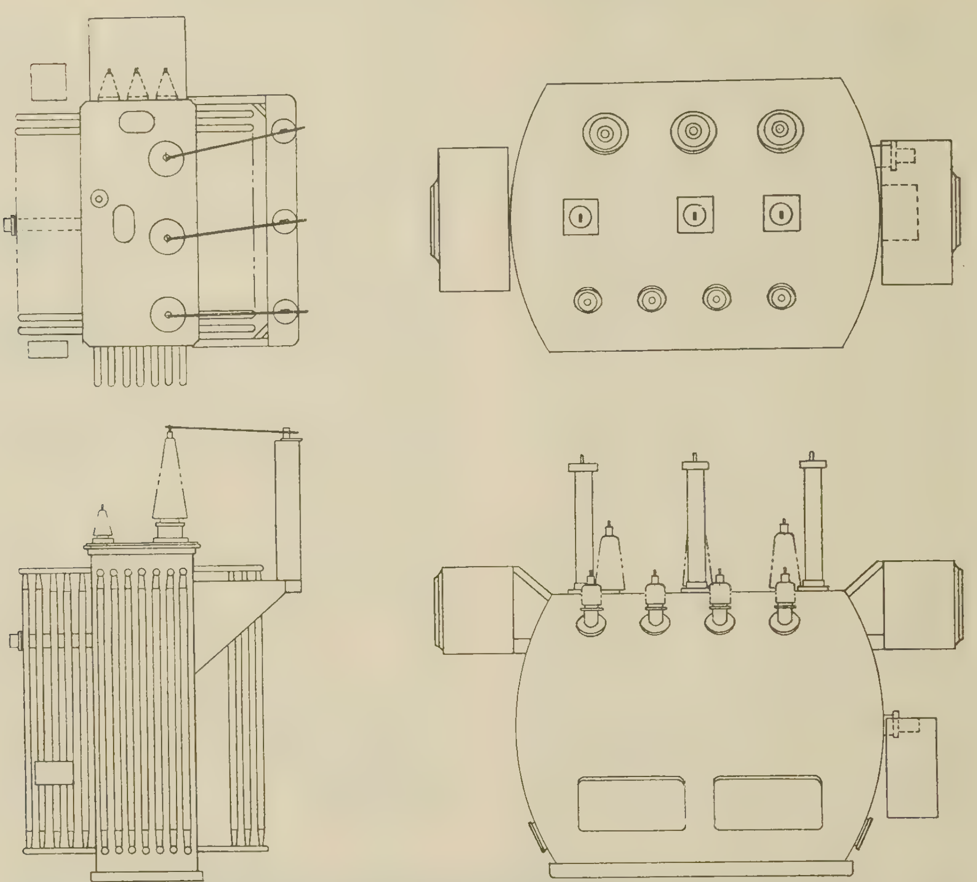

Fig. 11.—Comparative sizes of oil (left) and gas-insulated transformers, both rated 10 000 kVA, 69 kV.

done before the equipment will be competitive in every respect with liquid-insulated units, development is progressing and the results have been consistent and encouraging.

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RESISTIVITY MONITOR TO INDICATE OXIDE CONTENT OF SODIUM

By L. R. BLAKE, Ph.D., B.Sc., Associate Member

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SUMMARY

The electrical resistivity of sodium or sodium-potassium is continuously measured whilst flowing at operating temperature in a pipe, to provide an indication of impurity level, particularly oxygen, and thus to enable progress of clean-up in liquid-metal loops to be followed and to give assurance that high purity is achieved and maintained.

The meter is designed to be sensitive to a change of one part in 10^6 in oxygen level, corresponding to a change in resistivity of about one part in 10^4. A change in liquid-metal temperature of only 0·04° C will also produce this effect, and hence temperature compensation and achieving high stability are the chief problems of the design.

Two types of resistivity meter are described, both of which have been installed in the Dounreay fast reactor. Tests indicate that the design objective has been achieved and the value of the meter as an impurity monitor has been proved.

LIST OF PRINCIPAL SYMBOLS

f = Frequency, c/s.
I, i = Current, amp.
V, v = Voltage, volts.
R, r = Resistance, ohms.
X, x = Reactance, ohms.
M = Mutual reactance, ohms.
N, n = Transformer turns ratio.
W = Oxygen concentration in liquid metal, parts in 10^6 by weight.
T = Temperature, deg C or deg K.
α = Temperature coefficient of resistivity, deg C^{-1}.
β = Change in resistivity with oxygen level (parts in $10^6)^{-1}$.
ρ = Electrical resistivity, ohm-in or microhm-in.
τ = Comparator time-constant, sec.
θ = Output signal.

(1) INTRODUCTION

(1.1) The Use of Sodium and Sodium-Potassium as Nuclear-Reactor Coolants

Owing to their high specific heat and thermal conductivity, liquids make better heat-transfer fluids than gases, but their operating temperature range is much more restricted. For applications exceeding 400° C the number of suitable liquids becomes limited virtually to liquid metals if operation near atmospheric pressure is desirable. The low-melting-point alkali metals are particularly attractive heat-transfer fluids and can operate over a wide temperature range: from 98° C melting-point to 883° C boiling-point for sodium, −11° C to 784° C for eutectic sodium-potassium and 179° C to 1 317° C for lithium. Of these, lithium has the best heat-transfer properties but it is expensive and corrosive. Sodium is very cheap, it can be contained in steel and has good heat-transfer properties; it is now the most widely used of the three. Sodium-potassium is more expensive, it is chemically more reactive, whilst its specific heat is only 68% of sodium and its thermal conductivity only 34%; it is now used only when a low melting-point is essential.

Written contributions on papers published without being read at meetings are invited for consideration with a view to publication.
Dr. Blake is at the U.K.A.E.A. Dounreay Experimental Reactor Establishment.

Of all applications, the fast breeder nuclear reactor has greatest need of liquid-metal cooling. Water and organic coolants cannot be used because of their high neutron-moderating effect, as well as low maximum operating temperature. Gas cooling is just possible but only with the greatest difficulty if high heat-transfer rates are to be achieved, and this is vital if the core size and fuel cost are to be kept acceptably small; moreover, gas cooling is undesirable on safety grounds owing to the virtual absence of cooling should gas circulators fail. Only liquid metals remain as possible coolants for fast reactors; however, they fulfil this role admirably. For example, the Dounreay fast reactor is designed for about 50 MW heat extraction from a core only 21 in high and 21 in diameter, or 10 MW/ft^3; 70/30 sodium-potassium is used as coolant, entering the core at 200° C and leaving at 400° C. In the Enrico Fermi fast reactor the rating is even higher, namely 23 MW/ft^3 with sodium coolant entering at 288° C and leaving at 426° C.

One of the chief difficulties encountered in the use of sodium and sodium-potassium is attaining and maintaining a high purity, and, in particular, a low oxygen content. The presence of oxygen has a number of bad effects: it accelerates corrosion of steel and other metals; it causes blocking of pipes; it increases wear and galling of rubbing surfaces; and, owing to the presence of surface films, it can reduce heat-transfer properties and even worsen mechanical properties and reduce the performance of some types of electromagnetic pump. The routine now established to keep the amount of oxygen to an acceptable level is as follows:

(*a*) To blanket the liquid metal with inert gas of high purity (argon, helium or nitrogen with total impurities of less than 50 parts in 10^6 is often specified, although this can be relaxed under some operating conditions, particularly when the inert gas is static and when trace oxygen in it is not replaced after removal by the sodium).

(*b*) To ensure that the liquid-metal circuit is reasonably clean before filling. It should drain rapidly and completely, and 'dead-legs', where there is no through-circulation, should be avoided.

(*c*) To fill the liquid-metal circuit via stainless-steel filters of 5–15 micron pore size, repeating the procedure several times: heat the metal within the circuit to as high a temperature as possible, preferably above 300° C; drain into a dump tank and cool to near the melting point to precipitate the oxygen as sodium oxide; refill again via the filters through a pipe well below the liquid surface in the dump tank.

(*d*) To 'cold-trap' or 'hot-trap' the sodium continuously during operation.

A 'cold-trap' is a part of the pipe circuit through which sodium is circulated slowly at the lowest possible temperature; it utilizes the fact that the saturation level of sodium oxide falls sharply as the temperature is lowered, as indicated in Fig. 1. By keeping the cold-trap temperature below the lowest temperature in the loop and preferably around 150° C, sodium bled through the trap from the main loop will precipitate its oxide to a concentration appropriate to that temperature. The cold trap is filled with stainless-steel turnings, Raschig rings, sintered stainless-steel, wire-mesh or any other arrangement giving a high surface area on which the oxide can precipitate; it often includes a regenerative heat-exchanger to reduce the power needed to lower and raise the sodium temperature before it enters and after it leaves the cold trap. Typical cold-trap design practice

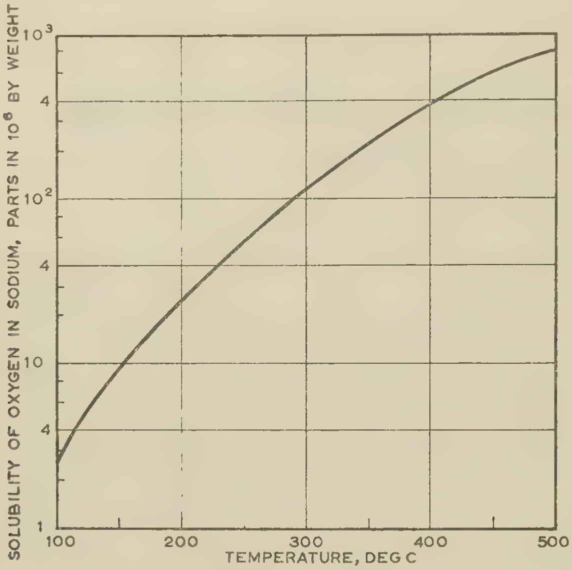

Fig. 1.—Solubility of oxygen in sodium.[11]

The curve is plotted using the relation

$\log_{10} W = 5{\cdot}27 - (1\,820/T)$

where W = Oxygen content by weight, parts in 10^6.
T = Absolute temperature, deg K.

calls for 1–10 ft² stainless-steel surface area per cubic foot of sodium in the complete circuit and a residence time of sodium in the trap of about 5 min; this lowers the oxygen concentration to near the cold-trap saturation level after the total sodium volume has passed about six times through the trap.

This cleaning and cold-trapping procedure enables the level of oxygen contamination to be reduced to a level of about 10–20 parts in 10^6, which is low enough to prevent pipe blockage and most of the other difficulties described above. Corrosion is also reduced to negligible proportions with certain metals, including iron, nickel, cobalt, chromium, molybdenum, tungsten and, in particular, 18–8–1 stainless steel. However, with refractory metals such as niobium, vanadium, zirconium and tantalum, 10–20 parts in 10^6 of oxygen still leads to excessive corrosion at temperatures above 400–450° C. Further reduction in the oxygen level to 10 parts in 10^6 or even less is possible by prolonged cold-trapping at low cold-trap temperatures of 100–150° C (98° C is the melting point of sodium), but a preferable method is 'hot-trapping', which in principle should reduce the oxygen level to much less than one part in 10^6.

A hot trap is similar to a cold trap except that it is run at high temperature; it utilizes the high affinity for oxygen of some metals at high temperature, such as zirconium, tantalum or uranium. With a continuous circulation of liquid metal through it, it is run at or above the highest temperature of the circuit, usually at 600° C–700° C; this reduces the oxygen to below the detectable limit. In other words, the hot trap utilizes the very defects of the metal needing protection, by sacrificing some of it at high temperature to save the working metal. In the Dounreay fast reactor, zirconium is used with 10 ft² of zirconium per cubic foot of liquid metal. Hot-trapping is usually commenced after cold-trapping: simultaneous operation is not permissible as this leads to oxygen transfer from cold to hot traps, oxygen being circulated in the loop during the process. Provided that the sodium system is well cleaned during initial filling operations, cold-trapping can be dispensed with altogether.

The value of hot-trapping has been demonstrated[1] by tests on niobium and vanadium, which are used to contain the fuel in the Dounreay fast-reactor fuel elements. For example, niobium corrosion was reduced by hot-trapping to 0·001 in/year compared with 0·04 in/year at 600° C under cold-trapped conditions.

To watch progress of this complicated clean-up procedure in the liquid-metal circuits of reactors, it is evidently highly desirable that instruments be provided to check both the oxygen level and the rate of corrosion.

(1.2) Measurement of Impurity Level

Methods so far proposed for measuring the concentration of impurities in liquid metals and their corrosive effects with refractory metals include the following

(*a*) Sampling and chemical analysis.[2]

(*b*) Oxide plugging meter,[2] in which the temperature of a perforated disc is gradually lowered until oxide is precipitated and liquid flow through it is prevented; at this point the oxide level corresponding to this temperature is read from a solubility curve as in Fig. 1.

(*c*) Corrosion meter.[3] This consists of two perforated plates, one niobium and one stainless-steel, inserted in parallel arms of a liquid-metal pipe circuit. The difference flow between the two circuits is measured over a given period, and the increase in the flow through the niobium plate indicates its rate of corrosion.

These methods have the disadvantage that continuous monitoring of impurity level is impossible. They are also rather complicated measurements and involve sooner or later breaking into the liquid-metal circuit, either to sample as in method (*a*) or to change plates as in method (*b*) or (*c*). Moreover, the lower detectable limit of the plugging-meter is about 8 parts in 10^6 controlled by sodium freezing, and with chemical sampling it is about 2 parts in 10^6, whilst representative sampling and avoiding contamination during sampling present additional problems.

The resistivity meter was developed to supplement these methods, and particularly to enable the progress of clean-up to be followed continuously and to give immediate warning should a leak develop in the liquid-metal circuit. It utilizes the fact that the presence of impurities in metals increases resistivity. Unfortunately, the change in resistivity of sodium and sodium-potassium with oxygen content was not known, though tests by G. V. Massey, at the U.K.A.E.A. Culcheth Laboratory had confirmed the presence of the effect though not its magnitude. The urgent needs of the Dounreay fast reactor, for which the instrument was required, made it necessary to design the meter on the indirect evidence of other metals and impurities, before laboratory tests could be made.

(1.3) Estimated Resistivity Change with Impurity Concentration

The fundamental expression for resistivity is shown by Zwikker[4] to have the form

$$\rho = \frac{2m}{e^2}\,\frac{v}{nL}$$

where m = Mass of the electron.
e = Electron charge.
n = Number of conduction electrons per unit volume.
L/v = Time between collisions of electron with the lattice.
L = Mean free path between collisions.
v = Effective velocity of electrons.

Zwikker argues that v changes little with temperature, whilst L is strongly temperature dependent, having two components:

$$1/L = (1/L)_{thermal} + (1/L)_{structure}$$

where $(1/L)_{thermal}$ is proportional approximately to the absolute temperature whilst $(1/L)_{structure}$ is constant. In an ideal lattice L should be infinite and ρ should be zero; the finite value of L comes from vibration of and imperfections in the lattice.

Table 1

EFFECT OF IMPURITY LEVEL ON THE RESISTIVITY AT ROOM TEMPERATURE OF COPPER, IRON AND NIOBIUM

						Ref. No.
Copper	Impurity	P	Si	Fe		
	$\frac{\text{Resistivity change}}{\text{Impurity change}}$	960	610	350		5
	Resistivity change per 1 part in 10^6 impurity change (10^{-9} ohm-in)	0·65	0·42	0·24		
Iron ..	Impurity	C	Si	Al	S	5, 6 and 7
	$\frac{\text{Resistivity change}}{\text{Impurity change}}$	250	140	120	100	
	Resistivity change per 1 part in 10^6 impurity change (10^{-9} ohm-in)	0·98	0·55	0·47	0·39	
Niobium	Impurity	O				
	$\frac{\text{Resistivity change}}{\text{Impurity change}}$	17				8
	Resistivity change per 1 part in 10^6 impurity change (10^{-9} ohm-in)	0·87				

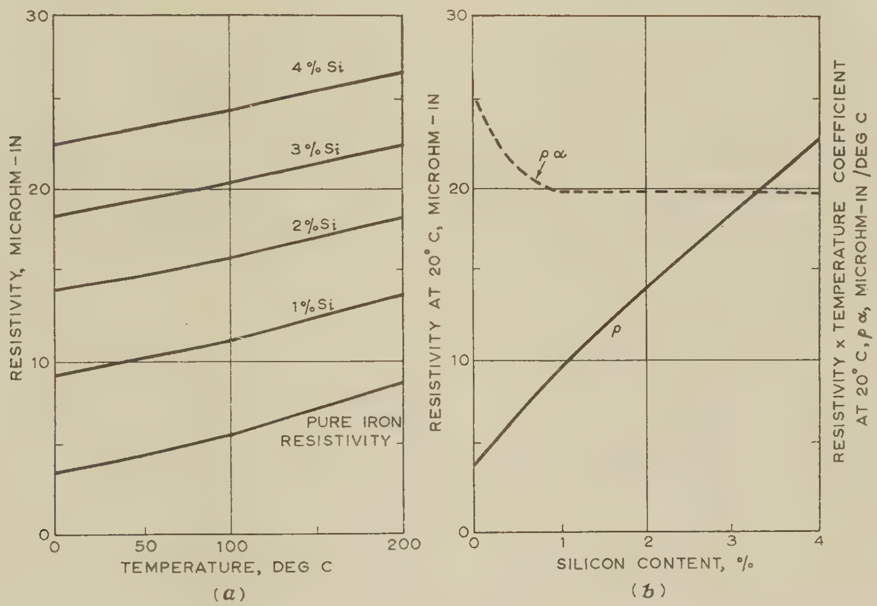

Fig. 2.—Variation of resistivity of iron with (*a*) temperature and (*b*) silicon content (References 5 and 7).

This explains qualitatively why resistivity, ρ, and temperature coefficient, $\alpha = (1/\rho)(d\rho/dT)$, of metals increase so markedly with impurity concentration, producing relative resistivity increases 100 to 1 000 times greater than their own proportion, as shown in Table 1.

The change in resistivity of iron, both with temperature and impurity concentration, is shown[6,7] in Fig. 2. It will be seen that the resistivity at temperature T and impurity concentration W, takes the approximate form

$$\rho = \rho_0 + \alpha_0 T + \beta_0 W \quad . \quad . \quad . \quad . \quad . \quad (1)$$

where ρ_0 is the resistivity of pure iron at 0° C, and the coefficients α_0 and β_0 are constant. This can be expressed alternatively

$$\rho(T, W) = \rho(T_1, W_1) + \alpha_0(T - T_1) + \beta_0(W - W_1)$$
$$= \rho(T_1, W_1) + \alpha_0\Delta_1 + \beta_0\delta_1$$

i.e. $$\rho = \rho_{(T_1 W_1)}(1 + \alpha\Delta_1 + \beta\delta_1) \quad . \quad . \quad . \quad . \quad . \quad . \quad (2)$$

where $\Delta_1 = T - T_1$, $\delta = W - W_1$, $\alpha = \alpha_0/\rho_{T_1W_1}$, $\beta = \beta_0/\rho_{T_1W_1}$; the coefficients α and β now change with temperature and impurity content.

In the absence of accurate data it was decided to design the meter, both for sodium and sodium-potassium, assuming that one part in 10^6 of oxygen increases the resistivity by about $0 \cdot 5 \times 10^{-9}$ ohm-in; the figures of Table 1 suggest a possible error of $+0 \cdot 5 \times 10^{-9}$ and $-0 \cdot 25 \times 10^{-9}$. The figures for

solid metals can be extrapolated to sodium and potassium with some confidence as their structure changes little from solid to liquid phase; moreover, the resistivity change through the phase change is small.

With the meters operating at 200° C this gives, for sodium,

$$\rho = 5{\cdot}17(1 + 0{\cdot}002\,63\Delta + 0{\cdot}000\,1W) \text{ microhm-in} \quad . \quad (3)$$

where $\Delta = T - 200(°\,C)$ and W is expressed in parts in 10^6. For 70/30 sodium-potassium at 200° C,

$$\rho = 13{\cdot}1(1 + 0{\cdot}001\,5\Delta + 0{\cdot}000\,05W) \text{ microhm-in} \quad . \quad (4)$$

This figures suggest that a 1° C temperature change produces a resistivity change equivalent to 20–30 parts in 10^6 of oxygen. Thus, in order to detect a change of one part in 10^6 in oxygen, it is necessary either to stabilize the liquid-metal temperature to within 0·04° C or to find a method of compensating for temperature changes, i.e. reducing the effective temperature coefficient 25 times if 1° C temperature change is permitted or 250 times if a 10° C temperature drift is permitted. It also involves reducing other spurious signals to a level of less than one part in 10^4. These are the main problems of the resistivity measurement.

(2) METHODS OF RESISTIVITY MEASUREMENT—GENERAL DESCRIPTION

(2.1) General

It is desirable to measure liquid-metal resistivity whilst circulating in a pipe at working temperature in order to avoid problems of sampling and to give an immediate and continuous indication of impurity changes. Under these conditions, d.c. methods of measurement can be dismissed as impractical owing to possible errors from thermal e.m.f.'s. A.C. measurements can present difficulties owing to pick-up and other inductive effects, but it is easier to eliminate this type of error. Measurement at frequencies above the normal power frequency has no obvious advantage, and moreover can lead to difficulties due to skin effect with pipe sizes above 1 in nominal bore. It was therefore decided to make the measurement at power frequency, possibly going to a lower frequency in the case of large-diameter pipes.

(2.2) Kelvin Double-Bridge Method

The most obvious method of measuring the resistivity of liquid metal flowing in a pipe is the Kelvin double bridge, as indicated in Fig. 3. This gives at balance $R_1/R_2 = r_1/r_2$, provided that the balance resistors r_1, r_2, r_3 and r_4 are made to satisfy the relation $r_1/r_2 = r_3/r_4$. Temperature compensation can be provided by making the resistors r_1 and r_3 have the same temperature coefficient of resistivity as the sodium-filled tube and by locating them in close thermal contact to it.

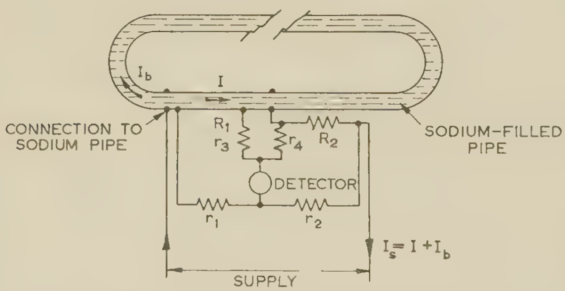

Fig. 3.—Kelvin double bridge for the measurement of resistivity of sodium flowing in a pipe forming part of a loop.

Alternatively, R_2 can be made temperature dependent, but this is much less desirable as it has to conduct the full supply current I_s and is therefore large in size and thermal capacity. The main difficulty foreseen with this method is due to the presence of the by-pass current I_b, which varies with change of main rig temperature or rig geometry; as I_b is not subject to control, it is difficult to compensate for variations in it. A magnetic core surrounding the pipe could reduce this current, but it is difficult to make it negligible.

(2.3) Current-Transformer Potentiometric Method

A preferable method of measuring the resistivity of a sodium-filled pipe connected in a loop is as shown in Fig. 4(*a*). Here a current transformer coupled with the pipe is used to derive a reference voltage v which varies with current I and not with I_s, as with the double-bridge method; fluctuations in I_b, therefore, are now no longer of importance. The current-transformer

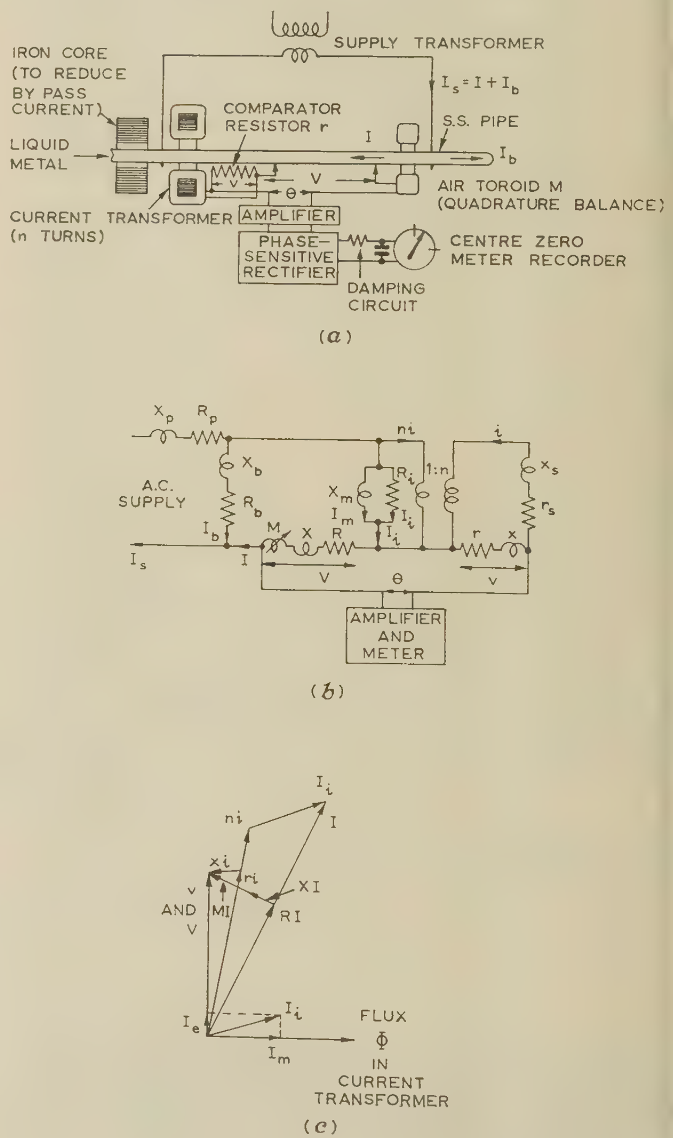

Fig. 4.—Current-transformer potentiometric method of measurement of resistance of liquid-metal-filled pipe and indicating or recording resistance change from a given value.

(*a*) Circuit arrangement. (*b*) Equivalent circuit.
(*c*) Vector diagram.

urden (the comparator resistance, r) is made to have a temperature coefficient equal to that of the sodium-filled pipe, and kept in close thermal contact to it. The equivalent circuit nd vector diagram of this method are shown in Figs. 4(b) and (c), rom which the balance equations can be derived. These equations can be simplified by expressing the current-transformer otal iron-loss current I_i (having loss component I_c and magnetizing component I_m) in the form $I_i = (A + B)ni$, where $A \simeq I_c/ni$ and $B \simeq I_m/ni$, provided that $x \ll r$. Then at balance, when $\theta = 0$,

$$[R + j(X - M)](I_i + ni) = i(r + jx)$$

Hence substituting for I_i in terms of ni and solving for the in-phase nd quadrature components

$$r/n = (1 + A)R - B(X - M) \quad . \quad . \quad . \quad (5)$$

nd

$$x/n = (1 + A)(X - M) + BR \quad . \quad . \quad . \quad (6)$$

gnoring the second-order effects (i.e. assuming that $A = B = 0$), change of liquid-metal resistivity, $\Delta\rho$, produces an output ignal θ, where

$$\theta/V \simeq \Delta R/R = (1 - R/R_p)\Delta\rho/\rho \quad . \quad . \quad . \quad (7)$$

nd where R is the parallel resistance of pipe and liquid metal n it, ΔR is the change in R due to the change $\Delta\rho$ in liquid-metal esistivity and R_p is the resistance of the pipe when empty.

Balance is achieved by tapping off a known proportion of V or v. In principle, it is unnecessary to make the mutual inductance M variable to balance out completely the quadrature components xni and XI, as the use of a phase-sensitive rectifier if excited from a source in phase with I eliminates these in a most convenient manner. Thus the non-variable air toroid, M, of Fig. 4(a) is adequate for approximate quadrature balance, which needs only to be to a level small in comparison with the maximum signal the amplifier can handle. Ideally, it is best to eliminate the quadrature component XI at its source by a compensating loop located close to the meter pipe to reduce the need for M and to reduce a.c. pick-up to the minimum.

(2.4) Dounreay Fast-Reactor Primary-Circuit Resistivity Meters

Two meters as shown in Fig. 5 are installed in the primary circuit of the Dounreay fast reactor. The meters were mounted in two cold-trap positions to avoid breaking into the primary circuit, which was complete and leak-tested at the time the decision to install the meters was made. This method of Fig. 5 differs slightly from that shown in Fig. 4 in that the current is induced directly in the liquid-metal pipe by linking the supply-transformer core through it; this is possible since the meter pipe and the sodium between the resistivity-meter container and cold-trap body form a convenient closed loop. It has the advantage that electrical connections to the pipe from the supply transformer are no longer necessary, and to some extent it reduces the effects of imperfect wetting of the pipe wall by the liquid metal. The absence of by-pass current I_b and the negligible magnetizing current of the supply transformer also permitted the air toroid of Fig. 3 to be replaced by a current transformer in the supply lead. A second current transformer was used to provide an electrical supply of correct phase for the phase-sensitive rectifier.

To permit adjustment of its temperature coefficient, the comparator resistance is made of two materials, one of high and the other of low or zero temperature coefficient. Nickel is used as one element since its temperature coefficient is the highest available and is close to that of sodium; Nichrome or Chromel is used as the other element of low temperature coefficient.

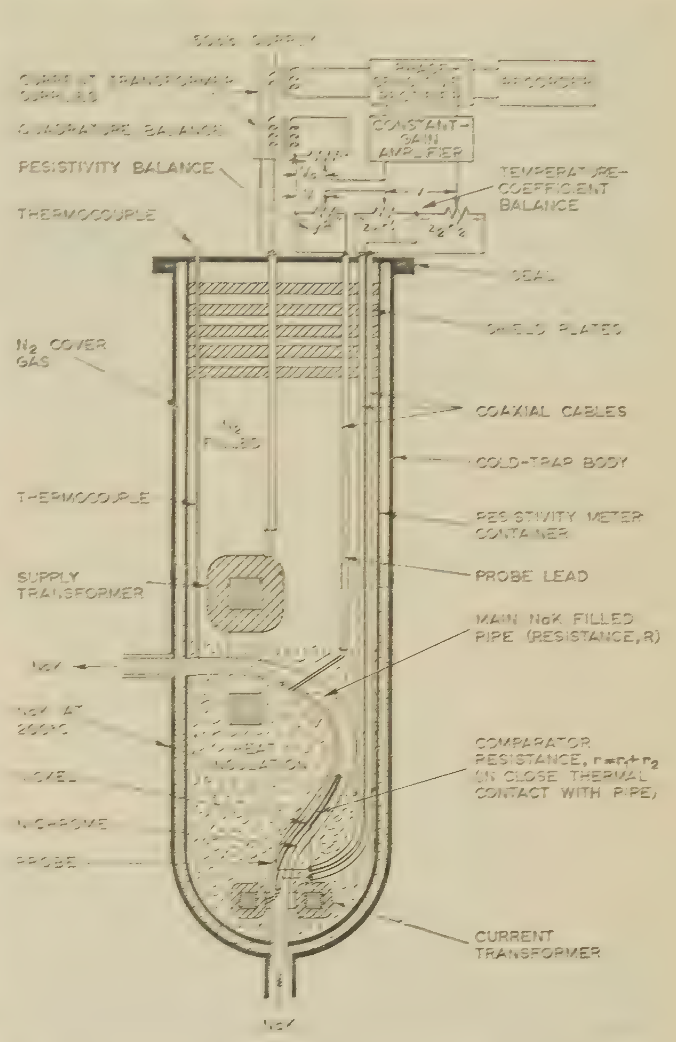

Fig. 5.—Dounreay fast reactor primary-circuit type of resistivity meter in which current is induced directly in the sodium-potassium-filled pipe.

R' = Balance potentiometer across R of Fig. 4.
r_1 and r_2 = Balance potentiometers across the high- and low-α components of the comparator resistor r.
y, z_1 and z_2 = Proportions tapped off.

Voltages of the appropriate proportion are tapped off the two wires using external potentiometers, the total voltage being kept constant during this process. The nickel and Nichrome compensating wires are electrically insulated from the pipe by 0·002 in thick mica sheet. This material appeared to offer the best overall characteristics of reliable insulation (with active sodium inside the pipe at high temperature) in thin sheet thickness, with reasonably high thermal conductivity; these characteristics are required to minimize the thermal time-constant of the comparator wire.

The comparator wires are held close and rigidly together, and the probe wire is held close and rigidly to the tube, in order to reduce the effective inductance and pick-up effects, particularly the change in inductive pick-up with relative movement of wires and tube. Connections to pipe and comparator resistor were brought out in Pyrotenax cable, with the outer sheath insulated and used as one of the leads to form a coaxial conductor, also to avoid inductive pick-up. Other methods of providing a comparator resistance of variable temperature coefficient are shown in Fig. 8.

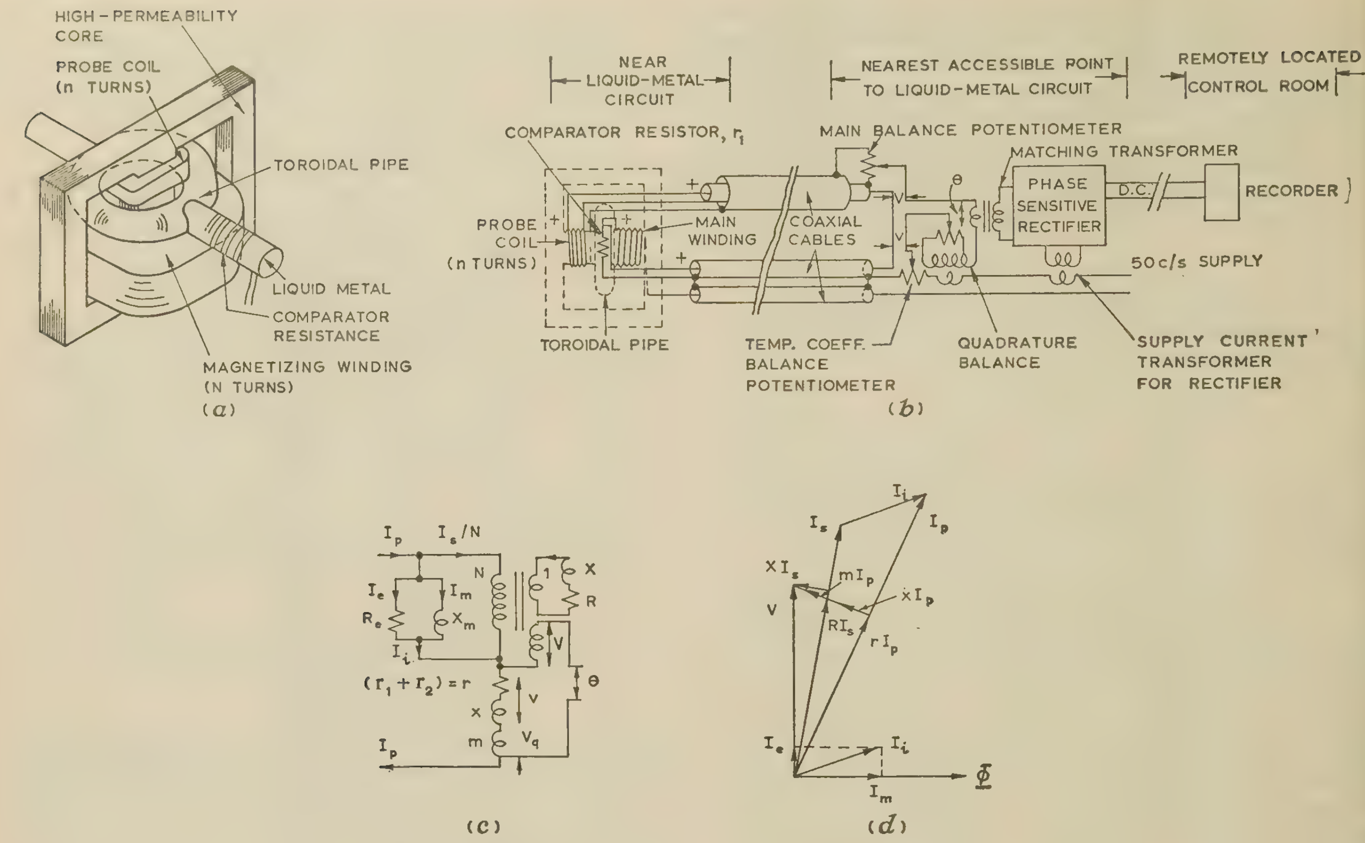

Fig. 6.—Toroidal-pipe-type resistivity meter.

(*a*) Physical arrangement.
(*b*) Circuit diagram illustrating method of making connections to avoid pick-up and reduce measuring circuit inductance.
(*c*) Approximate equivalent circuit.
(*d*) Vector diagram.

Both supply and current transformers are insulated with silicone-impregnated asbestos paper, asbestos being chosen so that the windings would stand a short-period temperature rise of the sodium to 400° C.

Balance is achieved by adjusting the recorder to zero by varying the potentiometers R', r_1', and r_2' connected across R and r. The arrangement of Fig. 5 is convenient in separating the two functions of temperature-coefficient adjustment and resistance balance; however, this makes it desirable to scale the potentiometer R' in terms of conductance or conductivity (rather than resistance or resistivity), either of the liquid metal alone or of the liquid-metal/pipe combination, at an arbitrary temperature. The recorder can be scaled to read resistivity or conductivity change from the level indicated by the balance knobs. It is useful to make the recorder with centre-zero and non-linear scale, with maximum sensitivity at the centre but a cramped scale near full-scale deflection; this gives maximum sensitivity to small changes and at the same time enables the magnitude of large changes to be recorded. The required non-linearity can be introduced either in the amplifier or at the recorder itself.

In the Dounreay fast reactor, recorders are provided also to monitor sodium flow and temperature in the main pipe. This is helpful if temperature-coefficient compensation is imperfect and to give indication of rate of sampling. The resistivity meter itself gives some check on flow, since, when a particle passes through a pipe, a pulse is shown on the recorder chart, the width of which indicates its time of passage between the probe points.

(2.5) Toroidal-Pipe-Type Meter

The toroidal-pipe resistivity meter illustrated in Fig. 6 is a logical development of the previous design in that the liquid-metal loop forming the supply-transformer secondary winding is reduced to the minimum with the object of conserving input power. A further considerable advantage is that it is no longer necessary to attach probes to the pipe. A 'probe coil' is used instead, linking the same flux as the toroid and having many turns, which considerably increases the signal voltage available and dispenses with the need for amplification. In turn, this improves reliability and makes pick-up leads and their resistance of lesser importance, thus reducing errors and restrictions on the location of the balance controls and indicators. A further advantage is that the comparator resistance can be smaller in diameter and of lower thermal time-constant. Imperfect wetting effects are also reduced should these be present, since no probes are used and negligible current passes through the pipe-wall/liquid-metal interface. The reduced input power requirements enable the transformer core to be constructed economically of high-permeability nickel-iron, thus enabling it to perform the dual role of supply and current transformer shown in Figs. 4 and 5. An essential feature of the transformer is the double-loop or shell-type construction, which enables the by-pass current to be eliminated. The disadvantages of the method are the greater complication of the toroid-pipe construction and the need to break into a pipe run to insert it.

The equivalent circuit and vector diagram of the toroid-pipe meter are shown in Figs. 6(*c*) and (*d*). As before, balance

equations are simplified by resolving the iron-loss current I_i into components in phase and in quadrature with respect to I_s. Writing $I_i = (A + jB)I_s/N$ it follows that, at balance,

$$[r + j(x - m)][(I_s/N) + I_i] = nI_s(R + jX)$$

where x and X are the leakage reactances of comparator resistor r and toroid pipe, respectively, with respect to the probe coil. Substituting for I_i and equating real and imaginary components, the balance conditions become

$$nNR = (1 + A)r - B(x - m) \quad . \quad . \quad . \quad (8)$$

$$nNR = (1 + A)(x - m) + Br \quad . \quad . \quad . \quad (9)$$

As in Section 2.3, ignoring second-order effects a change in liquid-metal resistivity produces an output signal θ, given by

$$\frac{\theta}{V} = \frac{\Delta R}{R} = \left(1 - \frac{R}{R_p}\right)\frac{\Delta\rho}{\rho} \quad . \quad . \quad . \quad . \quad (10)$$

where R_p is the loop resistance of the toroid pipe empty and R is its resistance full of liquid metal.

(3) DETAILED DESIGN OF CURRENT-TRANSFORMER METHOD

(3.1) Current-Transformer Design

The main design criterion for the meter is to achieve a large signal voltage to reduce amplification requirements with the minimum input power. This is best met by a main pipe of small diameter, but the tendency for pipe blockage during the loop clean-up is usually limiting. For this reason pipes below $\frac{3}{4}$ in nominal bore are not normally used in reactor systems, although a much smaller bore might be acceptable in a small loop built to high-vacuum standards. Normally the smallest pipe which operates at a moderately low temperature is chosen for the resistivity meter. The current I in the main pipe is limited to avoid excessive sodium temperature rise as it circulates through, otherwise rapid flow variations reflect rapid temperature changes which the comparator resistance cannot follow; it is generally desirable to limit the temperature rise to about 1° C for this reason and also to ensure that, should the flow fail, the temperature rise would not be excessive. The length of pipe between probe points is made optimum with respect to conflicting requirements of low input power and low amplification. The current-transformer iron-loss and magnetizing components can usually be made small by selection of high-quality core material with good interlaminal resistance for the current transformer and operating at a moderate flux density. Apart from this, the current-transformer design is conventional, the number of secondary turns being selected to give the desired comparator resistance, this being chosen to be as low as possible consistent with maintaining a low thermal time-constant of the comparator wire or strip with respect to the main pipe.

(3.2) Comparator-Resistor Design

It is shown in Section 1.3 that, for slow temperature changes, if the temperature coefficient of the comparator resistor is made the same as that of the sodium-filled pipe to within 0·4%, the sodium temperature need not be restricted unduly, a variation of ±10° C introducing an error signal of ±1 part in 10^6 oxygen equivalent. One method considered for achieving a closely similar temperature coefficient without the need for special adjustment and which would apply over a wide temperature range was to use as comparator resistor a scaled-down version of the main sodium-filled pipe; i.e. a capillary tube of the same material and wall-thickness/diameter ratio, filled with liquid metal of low oxygen content but otherwise of similar composition. An attempt was made to construct a comparator resistor in this way using a tube of dimensions 0·05 in outside diameter and 0·037 in inside diameter. This small size was used to keep small the thermal time delay of the comparator behind the main tube. However, it proved difficult to fill the tube without gas inclusions, and when using sodium-potassium it was also difficult to match its composition with that in the main pipe to within the limits necessary. This method was abandoned, therefore, and an accurately adjustable comparator resistor was employed, as described in Section 2.4, made up of high-temperature-coefficient (high-α) material such as nickel or platinum, in series with low-α material such as Chromel, Nichrome, or constantan, a suitable voltage being tapped off each for fine adjustment and added to or subtracted from each other.

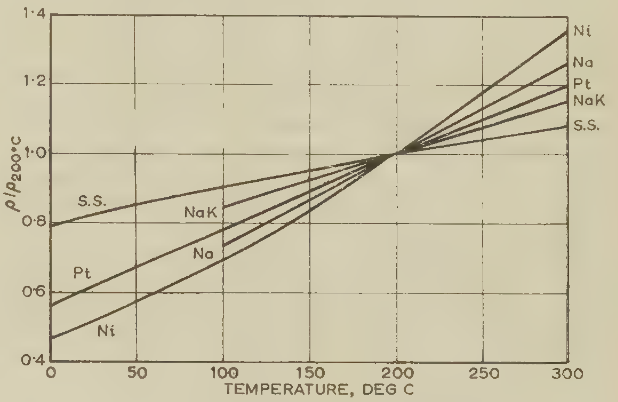

Fig. 7.—Variation of resistivity with temperature of some relevant metals.

Fig. 7 shows the variation of resistivity of relevant metals with temperature, normalized to unity at 200° C, the nominal working temperature of the meter. Table 2 also gives the actual resistivity at temperatures of 100, 200 and 300° C. There are unfortunately few solid metals having a temperature coefficient as high as that of sodium. All the transition metals have high values but platinum is just below that of sodium, and though the ferromagnetic metals, iron, cobalt and nickel, in this group have higher values, only nickel would appear capable of high-temperature operation in air for long periods without oxidizing; moreover, with all three there is substantial curvature of the

Table 2

REPRESENTATIVE RESISTIVITIES AT VARIOUS TEMPERATURES OF RELEVANT LIQUID-METAL CIRCUIT AND COMPARATOR RESISTOR

Metal	Resistivity at temperature			Temperature coefficient at 200° C
	100° C	200° C	300° C	
	10^{-6} ohm-in	10^{-6} ohm-in	10^{-6} ohm-in	deg C^{-1} × 10^4
Sodium	3·81	5·17	6·53	26·3
70/30 sodium-potassium..	11·1	13·1	15·1	15·28
Stainless steel (18/8/1) ..	31·6	35·0	38·0	9·15
Nickel	5·02	7·20	9·80	34
Platinum	5·37	6·89	8·27	21·1
Copper	0·894	1·163	1·431	23·1
Chromel (64 Ni, 25 Fe, 11Cr)	44·2	44·4	44·6	0·45
Constantan (60 Cu, 40 Ni)	18·89	18·90	18·94	~0·2

resistivity/temperature relation, so that temperature-coefficient balance applies only over a limited temperature range. Generally platinum and Nichrome would appear the most suitable components for use with 70/30 sodium-potassium, and two high-α components, nickel and platinum, would seem to be best for use with sodium; this arrangement reduces the amount of nickel and the associated non-linear effects to the minimum. Considerable non-linearity in the resistivity/temperature relation of nickel occurs near the Curie point (358° C), which makes it a most unsatisfactory high-α component over the temperature range 300–400° C. It is possible, however, to employ an artifice that enables platinum and Nichrome to be used with sodium, as shown later.

In order to balance the temperature coefficient, a suitable method is to oscillate the liquid-metal temperature by about a few degrees Centigrade at a few cycles per minute, using a heater at the inlet to the resistivity meter. The temperature-coefficient potentiometers can then be adjusted until no change in resistivity is recorded. This balance should be performed when the oxygen content of the liquid metal is below the saturation limit, since otherwise an additional change in resistivity is produced owing to the change in oxygen content with temperature. With sodium at 200° C, it is possible that this effect would prevent temperature-coefficient balance to better than 2%, whereas 0·5% or so is desirable.

Apart from very close temperature-coefficient balance, it is also necessary to make the thermal time-constant of the comparator resistance as low as possible. The approximate thermal circuit is as shown in Fig. 8. It is seen that the pipe wall itself

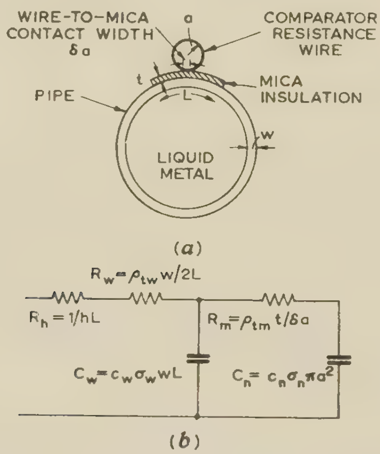

Fig. 8.—Thermal time-constant of comparator circuit relative to sodium in main pipe.

(a) Physical arrangement.
(b) Equivalent thermal circuit.

R = Thermal resistances.
C = Thermal capacitances.
R_h, R_w, C_w = Per-unit length of pipe and appropriate width L.
Suffixes w, m, n = Pipe wall, mica and wire, respectively.
ρ, σ, c = Thermal resistivity, density, and specific heat, respectively.
h = Sodium-to-wall heat-transfer coefficient.

has a thermal time lag on the liquid metal in the circuit, of the following order:

$$(R_h + R_w)C_w = (0\cdot1 + 0\cdot15)7\cdot6 = 2\,\text{sec}$$

for a stainless-steel pipe of 0·166 in wall thickness. The comparator resistance lags behind the wall with a further time-constant

$$R_m C_m \simeq \frac{20}{s} 0\cdot017 = \frac{0\cdot34}{s}$$

assuming 0·002 in mica and 0·018 in diameter nickel wire and where width of contact between nickel and wire is sr_n. Assuming $s = 0\cdot15$, then $R_m C_m = 2$ sec. In view of the irreducible value of 2 sec for the wall, there is little point in striving to reduce the comparator time-constant to much below this figure. Tests have been made on the meters which have substantiated a figure of 2 sec for $R_m C_m$.

(3.3) Effects of Imperfect Temperature Compensation

Ignoring second-order effects [i.e. assuming $A = B = 0$ in eqn. (5)] the balance equation (with main-pipe temperature T and temperature coefficient α, and comparator resistance temperature T' and temperature coefficient α') for two balance conditions, denoted by suffixes 1 and 2, is

$$r(1 + \alpha' T_1') = nR(1 + \alpha T_1 + \beta W_1)$$
$$r(1 + \alpha' T_2') = n(R + \Delta R)(1 + \alpha T_2 + \beta W_2)$$

assuming that it was necessary to change R effectively by ΔR for the second balance.

$$\text{Hence} \quad \frac{\Delta R}{(r/n)} = \frac{(1 + \alpha' T_2')}{(1 + \alpha T_2 + \beta W_2)} - \frac{(1 + \alpha' T_1')}{(1 + \alpha T_1 + \beta W_1)}$$

$$\text{or} \quad \Delta R/R \simeq [\beta \Delta W + \alpha'(T_2' - T_1') - \alpha(T_2 - T_1)]$$
$$= \beta \Delta W + \alpha' \Delta T' - \alpha \Delta T$$

Several conditions are of interest:

(a) Perfect compensation and temperature-following (i.e. $\alpha = \alpha'$ and $\Delta T = \Delta T'$).

Then $\dfrac{\Delta R}{R} = \beta \Delta W$, i.e. the proportionate resistance change is a direct measure of the oxygen content.

(b) Perfect compensation and imperfect temperature-following (i.e. $\alpha = \alpha'$, $\Delta T \neq \Delta T'$).

$$\text{Then} \quad \frac{\Delta R}{R} = \beta \Delta W + \alpha(\Delta T' - \Delta T)$$
$$= \beta \Delta W + \alpha(T_2' - T_2) \text{ if } T_1' = T_1 \text{ originally.}$$

(c) Imperfect compensation and perfect temperature-following (i.e. $\Delta T = \Delta T'$, $\alpha \neq \alpha'$).

$$\text{Then} \quad \frac{\Delta R}{R} = \beta \Delta W + (\alpha' - \alpha)\Delta T.$$

It thus appears that, even with perfect temperature compensation [case (b)], large error signals will be produced if the compensator does not follow the pipe temperature, the error being the same as that which would occur if no temperature compensation were provided ($\alpha' = 0$) and the pipe temperature were to change by the same amount between readings. Thus, if the time-constant of the compensator is τ seconds and the pipe changes by γ deg C per second, the difference in the temperature between compensator and pipe is $\gamma\tau$ deg C. To maintain an accuracy to within one part in 10^6 of oxygen,

$$\gamma\tau\alpha \leqslant \beta \quad \text{or} \quad \gamma \leqslant \frac{\beta}{\alpha\tau} = 1/26 \times 2\cdot0\,\text{deg C per second}$$
$$\simeq 1\,\text{deg C per minute}$$

If the liquid-metal velocity u through the meter were held constant and the comparator resistor centre were located a distance $d = u\tau$ before the centre of the pipe, the comparator would have advance warning of temperature changes and this error due to the time lag could be avoided; unfortunately u cannot usually be held

constant, and so reliance cannot be placed on this method. Apart from making the time lag a minimum, by reducing pipe-wall thickness and arranging the comparator in intimate thermal contact with the pipe, there would appear to be little more that can be done except reducing the temperature drift itself by automatic heating or a reservoir at the inlet.

(3.4) Other Sources of Error

(*a*) *Flow Changes.*—Resistivity heating by the current I in the main pipe produces a temperature rise of the sodium in it, which changes with change of flow rate. To reduce this effect it is desirable to use a low current density in the liquid and to employ a high flow rate. This has the additional advantage of increased rate of sampling of the main loop, but, on the other hand, it gives more rapid temperature variations in the meter if the main loop-temperature changes.

When a meter is being installed into an existing loop, the designer is usually not at liberty to select the rate of flow through the meter; however, the rate is not critical and a value between 1 and 15 ft/s is usually acceptable. In the design of Fig. 5 the current density in the main pipe is nearly 1 kA/in^2, and this gives rise to a 1°C temperature rise of sodium-potassium at 3 ft/s.

(*b*) *Supply Voltage and Frequency Changes.*—The method of measurement of Figs. 4–6 in principle ensures that supply-voltage variations do not affect the balance conditions and only change the output signal in direct proportion to the voltage change, which is quite acceptable. However, there are complications: it will be seen from eqns. (5) and (6) that the terms A, B, r, M and x appear in the conditions for balance; if these terms were constant they could be allowed for, but unfortunately supply voltage and frequency changes affect the ratios $A \simeq I_e/I$ and $B \simeq I_m/I$, owing to non-linearity of the dynamic B/H curve of the current-transformer core, and thus reflect changes in the balance conditions which are not acceptable.

These complicating terms must be made negligible by the use of high-permeability low-loss core material operated at moderate or low flux density. To appreciate the order of magnitude of the errors, values are substituted in the balance eqns. (5) and (6) appropriate to the design of Fig. 5. It is assumed that balance is obtained by tapping off a proportion y of the voltage across R, X and a proportion z of r, x. Then eqn. (5) gives

$$0{\cdot}66z = (1 + 000\,3)0{\cdot}57y - 0{\cdot}000\,7(0{\cdot}018y - M)$$

and eqn. (6) gives

$$0{\cdot}011z = (1 + 0{\cdot}000\,3)(0{\cdot}018y - M) + 0{\cdot}000\,7 \times 0{\cdot}57y$$

Of the two sources of error, $A \simeq I_e/I$ would appear to introduce a greater error than $B \simeq I_m/I$, thus making low loss more important than high permeability; however, this is offset to a certain extent since the eddy-current-loss component, I_e, is less voltage- and frequency-dependent than the magnetizing component, I_m. A further complication is that I_e also changes owing to changes of interlaminar resistance with time and temperature; high-quality high-temperature interlaminar resistance is therefore required for the current transformer.

Apart from supply-voltage and frequency changes altering I_m and I_e and hence the balance conditions, a further effect of supply-frequency change is to introduce a quadrature component into the output signal of value $\Delta\theta/V = 2(\Delta f/f)(I_m/I)$. In the design of Fig. 5 with $I_m/I \simeq 1\,000$ the quadrature voltage introduced following a 10% frequency fluctuation is $\Delta\theta/V = 2 \times 0{\cdot}1/10^3$, or 2 parts in 10^4. This is quite acceptable since the phase-sensitive rectifier further reduces this signal appreciably.

(*c*) *Skin and Proximity Effects.*—A further indirect effect of supply-voltage and frequency variations is introduced by variation in the comparator and pipe resistances themselves, owing to skin and proximity effects. A well-known formula for skin effect in a solid circular bar is

$$\frac{R_{ac}}{R_{dc}} = 1 + \frac{x^4}{192} + \frac{x^8}{4\,608} + \ldots \text{ where } x = \left[a\frac{4\pi\mu\omega}{10^9\rho}^{1/2}\right]R_{dc}$$

where a = Radius of wire, cm.
μ = Permeability of wire.
ρ = Resistivity of wire, ohm-cm.
ω = Angular frequency, rad/s.

If the proportionate resistivity change permissible is $\Delta R/R$ for a proportionate frequency change $\Delta f/f$,

$$\frac{\Delta R}{R} = \frac{x^4[(1 + \Delta f/f)^2 - 1]}{192}$$

$$\simeq \frac{x^4}{96}\frac{\Delta f}{f}, \text{ if } \Delta f/f \ll 1$$

Introducing appropriate values for μ and ρ, the maximum diameter of platinum wire or of a sodium-filled pipe at 200°C to reduce resistance changes to one part in 10^4 following 10% frequency change is about 0·8 in. Owing to its high magnetic permeability, the corresponding value for nickel is only 0·05 in.

The proximity effect between adjacent turns of comparator wire or between the comparator wire and the main pipe is of about the same order as the skin effect; if the pipe and wire sizes are small enough to avoid skin effect, the proximity effect is also usually negligible. However, if a nickel comparator wire is used, it is also important to locate the nickel wire away from high field regions or regions where the field is changing rapidly, say near transformer windings, otherwise resistance changes of the nickel can be much higher and will reflect both frequency and positional changes.

(*d*) *A.C. Pick-Up.*—Substantial error signals can be induced in the circuit loops or components of the resistivity meter by adjacent electrical apparatus. As these signals can closely resemble the signals produced in the meter by particles passing through, it is necessary to take special care to eliminate inductive pick-up to a low level, corresponding to about 10 μV for the straight-pipe meter and 1 mV in the toroid meter. To achieve this, all inductive loops must be eliminated in the meter wiring, as illustrated in Figs. 5 and 6(*b*), by using coaxial cable for all 'go' and 'return' wires to components, making sure that the coaxial cables themselves form no inductive loops. Where three connections are made to a component, such as the comparator resistor of Fig. 5 or the tapped probe coil of Fig. 6(*b*), either a double-coaxial cable is used as shown in Fig. 6(*b*) or two coaxial cables run together and with the outer sheaths solidly connected at each end as shown in Fig. 5. Leads close to components, if not in coaxial cable, should have 'go' and 'return' wires twisted together. To further reduce 50 c/s a.c. pick-up, all components should be magnetically screened in high-permeability nickel-iron boxes.

(4) DETAILED DESIGN OF TOROID-TYPE METER

(4.1) Transformer Core and Winding Arrangement

In order to reduce the by-pass current I_b in the main pipe run to a negligible level, it is essential to link the toroidal pipe with two core loops, as illustrated in Fig. 6(*a*). Two winding arrangements are then possible, as illustrated in Fig. 9, with two windings (one on each outer limb) or one winding (on the centre limb). It is readily proved from Fig. 9(*c*), that, if v_1 and v_2 are the voltages per turn associated with fluxes Φ_1 and

*

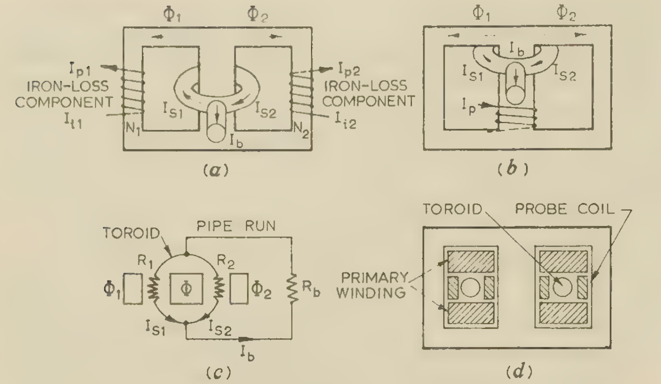

Fig. 9.—Possible winding arrangements of transformer for toroidal pipe meter.

(*a*) Coils on outer limbs.
(*b*) Coil on centre limb.
(*c*) Circuit showing factors controlling magnitude of by-pass current I_b.
(*d*) Preferable winding arrangement for low I_b, minimum leakage reactance between toroid and probe coil and also between toroid and primary winding.

Φ_2, respectively, in the two loops, the condition for zero by-pass current I_b is that $v_1/R_1 = v_2/R_2$, where R_1 and R_2 are the resistances of the two 'halves' of the toroid, divided by the main pipe run. If in Fig. 9(*a*) the coils on the two outer limbs are connected in parallel, the voltage across each is $V_s = v_1N_1 = v_2N_2$, when the condition for $I_b = 0$ becomes $N_1R_1 = N_2R_2$. Normally, the toroid with its pipe connections will be manufactured as symmetrically as possible with the object of making $R_1 = R_2$, in which case it is necessary to make $N_1 = N_2$.

In the alternative arrangement of Fig. 9(*b*) the primary winding current I_p consists of two components: the reflected toroid current I_s and the iron-loss component I_i. This is so for either iron circuit, loop 1 or loop 2. Hence, $I_p = (I_{s1}/N) - I_{i1} = (I_{s2}/N) - I_{i2}$. Thus the by-pass current,

$$I_b = I_{s2} - I_{s1} = (I_{i2} - I_{i1})N$$

irrespective of the geometry of the toroid. Since high-quality core material is used, it follows that NI_{i1} and NI_{i2} are much less than I_s, and hence I_b is very much less than I_s. Method (*b*) of Fig. 9 is perfectly satisfactory and has the advantage over method (*a*) that no special measures are necessary to ensure the correct ratio N_1/N_2, which requires that some control be exercised over the coil winder even when equal numbers of turns are required. It also has the lower leakage reactance.

(4.2) Size of Toroid Pipe and Transformer

The size of the toroid and its transformer is governed mainly by the size of error signal which can be tolerated, the chief of these being changes in balance conditions brought about by frequency or voltage changes, which alter the iron-loss current (and its components A and B) in a non-linear manner, as previously described. The better the quality of the core material, particularly in its loss characteristics, the smaller it is possible to make the toroid and the core laminations. It should be possible to reduce errors to about one part in 10^4 of the output signal with a toroid as small as 0·5 in pipe diameter and 2 in mean toroid diameter, using Supermumetal laminations of 0·004 in thickness.

In the Dounreay fast-reactor toroid-pipe meter design, it was necessary to locate the toroid in a $\frac{3}{4}$ in nominal-bore pipe, this being the smallest pipe available in the secondary circuit. An elliptical pipe was used of $\frac{13}{16}$ in $\times$ $1\frac{1}{2}$ in internal dimensions and $3\frac{3}{4}$ in mean toroid diameter. With this, the iron-loss component is one part in 10^3 of the toroid current in the case of sodium-potassium, and one part in 2×10^3 in the case of sodium, using ordinary Mumetal laminations. However, this is about sufficient to reduce error signals to below one part in 10^4 following 10% voltage or frequency changes.

(4.3) Comparator Resistor Design

In the toroid-meter design of Fig. 6, the variable temperature coefficient of the comparator resistor r is again provided by making it of two adjustable components: a high-α resistance r_1 in series with a zero-α resistance r_2. Unlike the case of Fig. 5 it is not possible to use potentiometers across them to effect balance adjustment, since this would involve a loss of signal power which cannot be tolerated if amplification is to be avoided. The method of Fig. 6 has the merit of simplicity but it has the following disadvantages:

(*a*) The lead connecting r_1 and r_2 introduces an unwanted and not necessarily temperature-independent resistance into the comparator; if r_2 is a long distance from r_1 this can be serious.

(*b*) In the Dounreay fast reactor, sodium-potassium is used as coolant, and thus platinum wire or strip for r_1 has a sufficiently high temperature coefficient (see Table 2 and Fig. 7) and an adequate range of temperature coefficient is controlled by the zero-α r_2. However, with sodium coolant it is necessary to resort to nickel for r_1, which introduces several complications as previously described, some of which are minimized by making r_2 of platinum and locating it also in thermal contact with the pipe.

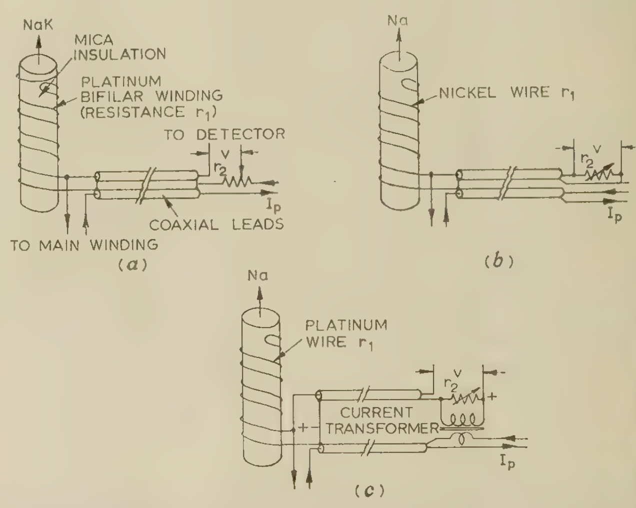

Fig. 10.—Methods of varying the effective temperature coefficient of the comparator resistance for the toroid meter design, the adjustment being remote from the liquid-metal-filled pipe.

(*a*) Series arrangement.
(*b*) Parallel arrangement.
(*c*) Current-transformer arrangement to raise effective temperature coefficient of platinum.

Fig. 10 summarizes some of the methods which are useful in various circumstances to make the temperature coefficient of the comparator resistance adjustable. The method of Fig. 10(*a*) is similar to that shown in Fig. 6 and can be used with sodium-potassium coolant provided that r_2 is not too far distant from r_1.

Fig. 10(*b*) shows another simple method useful if sodium coolant is used and if r_2 is not too remote from r_1. In this case, nickel wire is used for r_1, its temperature coefficient being adjusted by a parallel resistance r_2, which also has the effect of making the parallel combination more linear. For example, it is readily shown if the resistances of nickel wire at temperatures T_2, T_0 and T_1 are R_2, R_0, R_1, respectively, a resistance in parallel

across it of value $[R_2(R_0 - R_1) - R_1(R_2 - R_0)]/[(R_2 - R_0) - (R_0 - R_1)]$ will make the resistance curve of the parallel combination essentially linear over the temperature range $T_2 - T_1$. For nickel over 100–300° C, a parallel resistance of $2{\cdot}75R_0$ is about optimum, giving a temperature coefficient for the parallel combination of 25×10^{-4} °C^{-1}, which is close to sodium in a stainless-steel pipe.

Fig. 10(*c*) shows a current transformer used to obtain a voltage which can be subtracted from that across the platinum wire and thus raise its effective temperature coefficient to that of sodium. A further advantage of this method is that the cable now carries only the detector current, which is negligible at least near balance, so that the location of r_2 is now virtually unrestricted. A similar result can be achieved if the positions of r_2 and the transformer are interchanged, when the transformer becomes a voltage transformer.

The comments of Sections 3.2 and 3.3 on the calculation of the thermal time-constant of the comparator resistance, the method of temperature coefficient balance, and the effects of imperfect temperature compensation apply equally to the toroid-meter design, as also does Section 3.4. One important additional point is that pick-up is possible in the main transformer of Fig. 6(*a*), which should be screened.

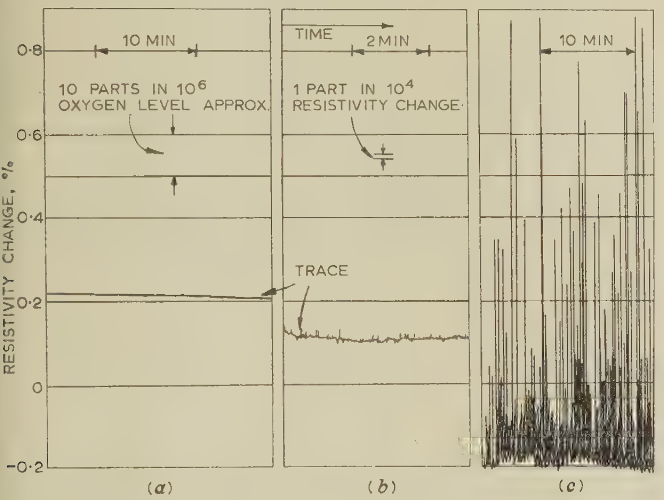

Fig. 11.—Examples of actual recorder charts under differing liquid-metal conditions.

(*a*) Trace during oxygen clean-up below saturation level.
(*b*) Trace with liquid metal above saturation level showing oxide particles.
(*c*) Gas entrainment.

(5) TEST BEHAVIOUR

Tests on the meter have shown that the desired stability of one part in 10^4 and temperature coefficient balance to 0·4% was achieved; in fact, a short-term stability of nearly one part in 10^5 was obtained. The variation of resistivity with oxygen content has been tested for 24/76 sodium-potassium. Test data suggest that for this metal the expression for resistivity [from a base temperature of 200° C, corresponding to eqns. (3) and (4)] is

$$\rho = 20{\cdot}1(1 + 0{\cdot}001\,3\Delta + 0{\cdot}000\,1\,W)$$

where $\Delta = T - 200$.
T = Temperature, deg C.
W = Oxygen content, parts in 10^6.

The oxygen-content coefficient of 0·000 1 appears accurate to within ±20%, assuming the solubility curve to be accurate. The change of resistivity with oxygen content is approximately as expected.

In view of these gratifying results the resistivity meter has proved capable of detecting changes in oxygen concentration of one part in 10^6. It has followed continuously the progress of oxygen clean-up in a rig; it has clearly shown the effect of changing rig conditions such as adjusting valves or changing cold-trap temperature, and it has also indicated a developing leak. Some representative curves obtained with the meter are shown in Figs. 11 and 12.

Almost as useful as the direct resistivity changes are the pulses shown up on the meter as oxide particles pass through [Fig. 11(*b*)]. It would appear that this could be utilized to make the resistivity meter act as a plugging-meter by lowering the temperature of the liquid metal at the resistivity-meter inlet until oxide was precipitated; when pulses appear this indicates the saturation temperature and hence, using a solubility curve like Fig. 1, the oxygen level. The resistivity meter could then be made self-calibrating.

The resistivity meter has also proved useful in detecting gas entrainment in rigs. If a gas bubble passes through the meter large pulses can be produced as shown in Fig. 11(*c*). Gas entrainment has been found by this means which has remained unsuspected before. It is quite possible that undetected gas entrainment as well as an indefinite oxygen contamination has been responsible for the wide spread of published liquid-metal heat-transfer data in the past. Normally gas entrainment cannot

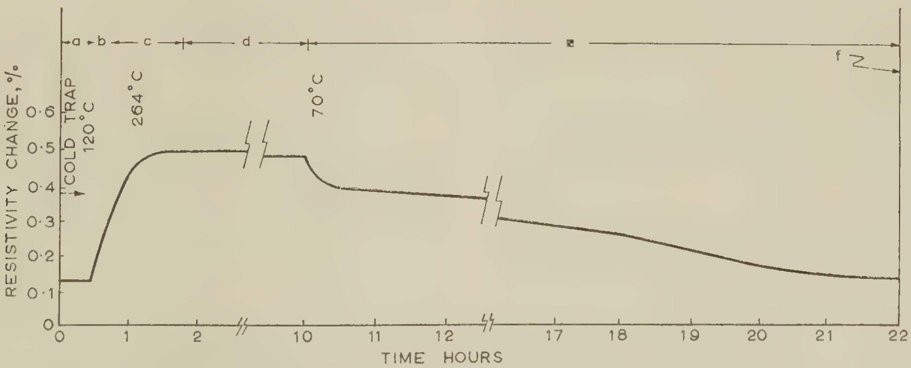

Fig. 12.—Resistivity changes due to change of cold-trap temperature in a 4 in-diameter circuit of 24/76 sodium-potassium.

(*a*) Cold trap at 120° C. 10 parts in 10^6 oxygen in NaK by chemical analysis.
(*b*) Cold-trap temperature rising.
(*c*) Cold trap held at 264° C. Resistivity rises as NaK becomes contaminated with oxygen.
(*d*) Cold trap valved off. Oxygen level remains constant.
(*e*) Cold trap reinserted and kept at 70° C. Resistivity falls with reduction of oxygen level.
(*f*) Chemical analysis indicates 7 parts in 10^6 oxygen. Resistivity reading, 0·09%.

be tolerated, and the origin of the gas must be found and stopped. Because of the utility of the meter for this work also, it is generally desirable to be careful in the method of tapping off the resistivity-meter liquid-metal pipe to ensure representative sampling of gas or oxide particles. This is best done from a vertical main pipe. It is also desirable to mount the resistivity meter pipe vertically to reduce the possibility of gas or oxide particles being trapped within the measuring section of the meter pipework.

(6) CONCLUSIONS

Of the two types of resistivity meter described, the toroid-pipe type of Sections 2.5 and 4 would appear generally to be preferred to the straight-pipe current-transformer type of Sections 2.3, 2.4 and 3, on the grounds of simplicity, cheapness, lower power requirements and greater output signal. A toroid-pipe meter has been installed in each of two secondary circuits of the Dounreay fast reactor and a straight-pipe type in each of two primary circuits. Both meters have performed satisfactorily with errors kept to below one part in 10^4 approximately, thus enabling a resistivity change of $0{\cdot}01\%$ to be measured; tests with 24/76 sodium-potassium indicate that this corresponds to an oxygen level of one part in 10^6. A fall of oxide level to the saturation level is indicated when small pulses or 'noise' on the chart disappear, the pulses being caused by oxide particles passing through the meter. It is hoped that the resistivity meter will become a standard instrument in liquid-metal loops in which low impurity level has to be achieved and maintained.

(7) ACKNOWLEDGMENTS

The author is particularly indebted to Mr. A. R. Eames, Dounreay Experimental Reactor Establishment, who constructed and tested the resistivity meters described, and to the Research and Development Branch, Windscale Works, United Kingdom Atomic Energy Authority, who provided loop facilities for testing the meters. Acknowledgment is also made to United Kingdom Atomic Energy Authority Development and Engineering Group Headquarters for permission to publish the paper.

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DISCUSSION ON 'THE SELECTION OF INSULATION LEVELS AND TESTS FOR HIGH-VOLTAGE TRANSFORMERS'*

Before the North-Western Supply Group *at* Manchester, 20*th October*, 1959, *and the* North Staffordshire Sub-Centre *at* Stafford, 18*th January*, 1960.

Mr. E. W. Connon (*at Manchester*): An important part of the paper is concerned with the difficult question of rod-gaps versus surge diverters for insulation co-ordination, and in Section 5.1 the author lays down the basic requirement that a rod-gap is only satisfactory if the arrangement of the system is such that the earth fault which results from its operation will not affect continuity of supply. In considering the results of service experience in Section 5.3 he refers to the fact that, in general, rod-gap operation does not cause an interruption of supply.

This is, of course, basically true, in that the Grid system is designed so that the outage of any one piece of equipment can generally be tolerated without affecting continuity of supply. But it ceases to be true as soon as any piece of equipment has to be taken out of service. The rapid growth of demand has led to very substantial outages of all kinds of equipment for construction and reinforcement purposes, and maintenance work is also necessary. There is a non-availability factor due to these causes which itself amounts to about 2%. In addition, records from a part of the Grid system show that about 40% of the interruptions of supply take place when some relevant piece of equipment is out of service for maintenance or some other purpose; in other words, they occur during the 2% non-availability period.

A further point of importance is that, with the general tendency to bank several transformers on one circuit, a rod-gap operation on one transformer causes the outage of several, leaving the supply at several points vulnerable in the event of a subsequent lightning stroke occurring before reclosure. These facts are not brought out in the survey of service experience or in Table 6, and it would be interesting to have the author's views on their relevance.

The author has shown that surge diverters, by reducing transformer insulation levels, could reduce transformer prices. It would be interesting to consider whether the reduction in interruptions of supply would provide a further economic argument for surge diverters. This, of course, would require an assessment of the financial losses resulting from supply failures, and that is singularly difficult to deduce.

Mr. H. L. Thomas (*at Manchester*): With reference to the

* Harper, G. B.: Paper No. 2804 S, January, 1959 (see 106 A, p. 429).

oltage/time characteristic of transformer insulation, the author raws attention to the substantially constant breakdown strength f insulation for times to breakdown in the range 3 microsec to everal thousand microseconds. In the chopped-wave test on a ransformer, a voltage of 115% of the full-wave level is applied or approximately 3–4 microsec, and it is stated in the paper that his implies enhanced insulation strength over that required for he specified full-wave level. This conclusion results from an ver-simplification of the problem, for we are not dealing with sample of insulation with voltage applied between two elec-rodes, but with a complicated structure involving inductive vindings with different voltages of varying waveshapes across a nultiplicity of insulation paths and at different times. For the uration of application of the chopped-wave voltage only part f the major insulation is fully stressed, owing to the wave not aving penetrated far into the winding in this time, whilst nternal voltages, such as those across tappings in the middle or owards the neutral end of the winding, are only fully developed y longer applied waves, as in the full-wave test. Therefore it annot be inferred that there is necessarily any inherent overall dditional insulation strength over that required to meet the ull-wave test.

For an initial determination of protective-gap settings, eference is made to relative voltage/time curves, although the nadequacy of a single curve to represent transformer impulse nsulation strength, coupled with inherent inconsistency of rod-ap operation, makes this at best only a rough guide, and the etting finally adopted is usually decided by system operating xperience. With surge diverters, however, the almost flat voltage/time characteristic of the gap sparkover greatly simplifies matters, since, if the protective level for full-wave conditions is chieved, still greater margins will follow for shorter times.

Mr. F. S. Edwards (*at Manchester*): The last three lines of Section 3.3.2 are slightly ambiguous. I take them to mean that he 15% over-voltage on the chopped-wave test represents an ncrease in the severity of the test of at least 10%, as compared with the full-wave test; i.e. not more than 5% reduction in everity is allowed because of the short time of stressing.

In Section 6.3 it is stated that the reduction in present values (of power-frequency test voltages) would permit a longer duration of application, which would not involve the same degree of undetected damage. (The undetected damage here referred to is presumably that caused by the test and not pre-existing damage which it is the object of the test to detect.) Is this statement necessarily true? If a test is to be effective, whether of long or short duration, it must be sufficient to cause breakdown, either partial or complete, of any defective insulation. The most suitable time of stressing which would reveal defective insulation with certainty, but without damaging sound insulation, has been n dispute for many years, and I did not know that any final conclusion had been reached.

Mr. F. Mather (*at Manchester*): The condition of effective earthing, corresponding to an earthing coefficient of 80%, now appears in various British Standards. In my experience the condition is rarely encountered in systems below 132 kV, and I imagine that, even at this voltage, the coefficient will seldom be below 80%.

The Introduction suggests that, whenever a rod-gap sparks over, protective-gear operation is necessary to interrupt the power-follow current. The author will no doubt have noted French experience, recently published, in which the employment of numerous oscilloperturbographs has shown that over 50% of phase-to-earth faults are self-extinguishing.

In Section 5.3 the assumption is made that all lightning faults could be prevented by installing sufficient surge diverters, but it appears from Table 6 that 56 out of every 60 circuit interruptions due to lightning are caused by local flashover on the lines. The figure would appear to be a function of the lightning currents and local earthing impedances and therefore unaffected by the number of surge diverters at terminal equipment.

Mr. J. Wainwright (*at Stafford*): Despite the development of more reliable methods for indicating faults during impulse testing, there is still the possibility that a unit may be put into service with its insulation partially damaged by the test. For this reason, it is of great importance to know how those units which have been impulse tested have fared subsequently in service. Of the 59 units mentioned in Table 7, how many have failed after being commissioned?

There are two further points regarding the breakdowns in service mentioned in the last paragraph of Section 6.2. First, were these three transformers subjected to impulse tests before dispatch, and, secondly, is it to be inferred from the first sentence in this paragraph that the breakdowns were in some way brought about by the power-frequency test at the works?

With regard to test (*b*) in Section 6.3, one of the large manufacturers in the United States has been advertising the fact that, for several years, all their large power transformers for 115 kV or above are subjected to a synthetic switching surge test. This is performed by arranging for the test voltage to rise rapidly until it is chopped at the required value by a sphere-gap.

Mr. J. H. C. Peters (*at Stafford*): Most, if not all, modern designs of air-blast and bulk-oil circuit-breakers for voltages of 132 kV and above employ some form of resistance switching, and within certain specified limits this controls the over-voltage produced when the circuit-breaker is switching an unloaded transformer. The usually accepted maximum over-voltage is 2·5 times the normal peak phase-to-neutral voltage, but this value is very seldom reached, and in most cases, the over-voltage produced is less than twice. The maximum figure of 2·5 applied to the maximum system voltage associated with the 132 and 275 kV Grid system gives values of 300 and 600 kV, respectively, which are well within the reduced impulse levels proposed.

Hence, if resistance-switched circuit-breakers are used a special switching surge test appears to be unnecessary. Non-resistance switched circuit-breakers, however, can give rise to very much higher over-voltages, and hence the remedy appears to lie in the correct choice of circuit-breaker rather than in the addition of a special test.

Mr. H. E. Pettitt (*at Stafford*): Table 5 refers almost entirely to transformers of unknown impulse strength, and therefore has no direct relevance to the question whether the present insulation test level can be reduced. Direct comparison of Tables 5 and 6 condemns the rod-gap as a protective device; 50% of rod-gap operations are coupled with transformer failure. However, by restricting the fault statistics to impulse-tested designs we reach a very different conclusion. The very low incidence of rod-gap operation, namely 0·8% per annum, together with fact that the supply is not usually interrupted by this operation, suggests that trials could be made with a gap of less than 26 in.

I will confine my main remarks to Section 6.3. With regard to insulation the designer's aim is 100% reliability achieved with the minimum cost of material and labour, with restricted size or weight as extra complications in an ever-increasing number of cases. As a check on this expected reliability, the accepted test levels which the transformer has to meet must be related to service conditions. On transformers with taps (particularly wide-range on-load tap changing) it is, however, not always possible to test all insulation factors on one test on one tap only, so that the integrity and skill of both the designer and shop labour will always have to be accepted to some extent.

I offer the following comments on the suggested procedure;

(*a*) There may be an interim period when tests of a simplified character, or perhaps even reduced level, are used as routine, with sampling tests only at full level, but this will eventually change to a standard routine basis only.

(*b*) Proposals so far have glossed over fault detection. Much investigation has to be done on this problem.

(*c*) I agree with the author. Table 27 of B.S. 171: 1959 gives a time/voltage variation from the standard. If we take a 15 min period, which is the longest tabled, the test on the 132 kV service, 550 kV test level units would be 131 kV, which is 56% above the maximum normal working voltage to earth and 13% above the maximum voltage to earth under fault conditions. 13% may seem quite low, but it is coupled with a long time factor relative to fault conditions. Would the author specify this variation instead of the standard test? In respect of this, or any reduction in power-frequency tests, I do not think that any costs will be altered, since they are already being fixed by the impulse level required.

Much research is now being done on the detection of corona by measurements at radio frequencies. When sufficient knowledge has been gained in this field it will be possible to reduce the low-frequency test level to something much closer to actual operating voltages.

Mr. G. B. Harper (*in reply*): Due account must be taken of the factors mentioned by Mr. Connon in assessment of the consequences of possible outage resulting from rod-gap operation, although the same factors affect many other aspects of system design. The overall influence of any increased liability in this respect is already shown in the comparison of non-availability given in Section 5.3, which covers all transformer outages, and, considering service experience, it would appear that no noticeable embarrassment is caused. Comments on the use of surge diverters to improve non-availability are also included in Section 5.3.

Mr. Thomas rightly draws attention to the limitations of a single voltage/time characteristic as an accurate representation of all the circumstances associated with a complex insulation structure and also to the difference between stresses arising during full- and chopped-wave impulse voltage tests. Appreciating these limitations and differences, I suggest that a single characteristic is adequate for the purpose of insulation co-ordination and that, for the same purpose, an assumed increase in transformer insulation strength of at least 10% over the full-wave level due to the higher chopped-wave voltage is permissible.

With regard to Mr. Edwards's comments on power-frequency voltage tests, I would confirm that the undetected damage mentioned is that which might be caused by the test. Suggestions for increased duration, however, are coupled with proposals for a complete re-examination of present procedure, and, for such revision, the suitability of any one particular form of test must be considered in relation to the severity and purpose of the others.

The assumption in Section 5.3, referred to by Mr. Mather, applies only to transformer outages and was assessed directly from the actual number of hours lost due to transformer faults associated with lightning and switching. I agree, however, that rod-gap operation does not necessarily involve a power-follow current.

In reply to Mr. Wainwright, none of the 59 transformers listed in Table 7 have failed in service, and the three units mentioned in the last paragraph of Section 6.2 were not subjected to impulse test before despatch. The precise cause of the latter failures is not known.

Table 5 surveys all faults due to lightning or switching, and, as implied by Mr. Pettitt, if confined to modern designs, considerable improvements would be indicated. Direct comparison between Tables 5 and 6, for a measure of rod-gap performance, however, is not valid, as, in addition to the influence of older transformers, the particular circumstances of each fault recorded in Table 5 must be considered. Actual rod-gap settings at present used have been derived from experience with various settings. With regard to proposals for future test procedure and the reduction of power-frequency voltage tests in accordance with Table 27 of B.S. 171: 1959, I would refer to my reply to Mr. Edwards. The introduction of a reliable technique for a corona measurement would have a considerable influence on any future consideration of dielectric test procedure.

DISCUSSION ON 'ELECTRIFICATION OF THE U.K.A.E.A. INDUSTRIAL GROUP FACTORIES'*

SOUTH MIDLAND CENTRE, AT BIRMINGHAM, 2ND NOVEMBER, 1959

Mr. G. F. Peirson: Being in one of the nationalized industries where capital expenditure has been sorely restricted, I studied with some envy the authors' lavish provision of standby equipment. If they were starting now with their present experience, would they still provide the duplication which was included in the original designs for the atomic plants?

Dr. D. N. Truscott: Will the authors elaborate on the service experience of motors with silicone-insulated windings? Have they been entirely satisfactory or were there many initial troubles which have been overcome?

Mr. G. S. Buckingham: It is obvious from the size of the sources of power supply which are feeding Capenhurst that there must be a difficult short-circuit problem. What have the authors done to deal with the possibility of heavy short-circuits on their mains? The high-voltage switchgear is rated at 350 MVA in what one might call a simple substation and the medium voltage short-circuit capacity is 31·5 MVA. A short-circuit would have dire consequences on such plant. It would be interesting to know whether any special steps have been taken to deal with this.

As I judge it, this plant is designed to take large volumes of mixed gases at one end and a small volume of the gas actually required comes out at the other end. What happens to the remainder?

Mr. R. A. Joseph: At Capenhurst there is a large amount of power and there must be considerable I^2R losses from transformers, cables, motors, etc. Have any provisions been made for dissipating this heat from the buildings?

Mr. J. Terry: In Section 5.5 there is reference to the use of double-wound transformers with earth shields and a description of the method for obtaining low-voltage supplies for portable tools using double-wound transformers with the secondary

* Binns, J. W., and Outram, W. J.: Paper No. 2698 U, August, 1958 (see 106 A, pp. 77 and 448).

winding earthed at its mid-point—which arrangement I think to be quite satisfactory. Are there special reasons for the inclusion of earth shields in this application?

Mr. I. G. Edwards: I should like details of the β-quenching and α-annealing processes and of the methods used for the removal and disposal of faulty glove-box equipment. What is the volumetric ratio of enriched to unenriched gas in the diffusion process?

While the power stations supplying Capenhurst have Grid interconnection, this desirable feature does not obtain at substations A–D, where an associated section of plant would be shut down in the event of total failure of supply at one main substation due to outage of both 132 kV incoming feeds. Probably reasons against provision of local 132 kV interconnection might be:

(*a*) Existing generation and transmission resources might be unable to cope with the additional burden put upon them by the heavy load transfer across an interconnector under single-substation shut-down conditions.

(*b*) Undesirability of parallelling two generating stations through an interconnector.

(*c*) An increase in short-circuit level at Capenhurst.

There are perhaps other and more cogent reasons than these; if so, will the authors give them?

Are the electrical supplies to the main blowers and the operating mechanisms of the moderating rods interlocked so that a failure of supply to the main blowers would automatically shut down the pile? What is the size of the shut-down fans which have then to take over?

Mr. E. Bramald: Apart from the special gland shown in the illustrations of cable terminations, are there any special techniques which have been developed? I notice a number of mass-impregnated non-bleeding cables; are normal compounded terminations used and is there any special technique in relation to their terminations in inverted positions?

Mr. J. Hamilton also contributed to the discussion at Birmingham.

Messrs. J. W. Binns and **W. J. Outram** (*in reply*): Our earlier replies amplified the factors taken into account in arriving at the present Capenhurst system and cover the main points raised on this subject by Mr. Edwards, with the exception of those on blowers. Loss of the main blowers does cause reactor shut-down, and in the original Windscale reactors the shut-down fans were of 7½ h.p.

We cannot accept Mr. Peirson's suggestion that the provisions made for standby equipment have in any way been lavish. When full consideration is given to the cost of failure of supply, we are of the opinion that what has been provided is, in fact, not more than that dictated by a normal prudent insurance policy.

With reference to Dr. Truscott's request for information on service experience on motors with silicone-insulated windings, while we have some of these in service, they have not been in use for sufficient time to have given us any real experience. We have, however, generally been free from any initial troubles.

Mr. Buckingham asks what special steps have been taken to deal with heavy short-circuits resulting from the large fault powers necessitated on certain of our systems: these have been primarily to design adequately to meet the conditions; cable ratings have been chosen to withstand through faults; ring main and interconnection cables are fitted with unit protection; spur feeders have, in general, conventional o.l./e.l. protection; medium-voltage circuits have judiciously located h.r.c. fuses, earth-leakage relays, etc.

With reference to Mr. Joseph's question on the I^2R losses at Capenhurst, the heating due to these has certainly been a problem, to overcome which a large proportion of the plant has water-cooled motors. Extensive ventilation systems are installed. In addition, the building is of a simple sheeted design, thus permitting fairly large natural heat losses.

Mr. Terry questions the use of earth shields as well as earthing the neutrals of the l.v. windings of low-voltage transformers: this has been done at little extra expense and is in line with the precautions taken to minimize the hazard from the use of portable tools.

On the question of cable terminations raised by Mr. Bramald, we do not encourage the use of inverted terminations of paper cable; where this is necessary, special attention is paid to the design of the box. Regarding the compound used, we are tending to favour the use of semi-fluid types.

A number of other questions have been asked relating to the processes at Capenhurst and other factories which, it is regretted, cannot be answered.

DISCUSSION ON 'THE DELTIC LOCOMOTIVE'*

Before the WESTERN UTILIZATION GROUP *at* BATH, *9th November*, 1959, *the* SOUTH MIDLAND CENTRE *at* BIRMINGHAM, *1st February*, *and the* TEES-SIDE SUB-CENTRE *at* MIDDLESBROUGH, *2nd March*, 1960.

Mr. G. Brombley (*at Bath*): One of the important aspects of the Deltic engine is that its output gives the electrical designer a difficult problem. The generator rating of 1 200 kW is probably the maximum which can be achieved at 1 500 r.p.m., and whereas it is possible to get more out of the engine by running it faster, the generator output might need to be reduced at higher speeds, owing to commutation difficulties. Are there any special features of the armature winding on the present generator?

The use of a self-aligning roller bearing follows normal traction practice, but the use of two engines in one locomotive could give rise to 'brinelling' of the generator bearing when one engine was stationary. Has any such trouble been experienced?

* COCK, C. M.: Paper No. 2769 U, December, 1958 (see 106 A, p. 107).

It is noted that each engine has its own governor and each generator has its own torque regulator; there are thus two devices which determine the speed of the power unit. What effect has this upon the parallel operation of the auxiliary generators, and are any special devices needed to ensure satisfactory parallel running?

One surprising feature of the Deltic locomotive is that, although it has two power units, they rely upon one battery for starting. The risk of a battery becoming discharged through faulty circuits is so great that one would have thought that the increased reliability of fitting two batteries would have justified the extra cost and weight involved.

The electrical circuits include protection against wheel slip,

and although very little extra equipment is involved, it nevertheless means increased complication. I wonder whether this is really justified, and it would be interesting to know whether similar features are included in Diesel-hydraulic locomotives.

Mr. H. M. Fricke (*at Birmingham*): Will the author indicate how the loads per axle vary during acceleration and retardation?

Mr. L. L. Tolley (*at Birmingham*): I have always believed that the difficulty with electric traction was that of the bogies hammering the rails, and I am interested to see the author's explanation of this. Does this imply that the hydraulic drive is likely to be a very strong competitor to the electric drive? Does the hydraulic drive produce a similar hammering? I presume that hydraulic motors are lighter than electric ones, since they do not have magnetic circuits.

Mr. H. J. Davies (*at Birmingham*): In order to produce an economically designed electrical transmission for Diesel locomotives it is necessary to allow some unloading (i.e. to depart from the constant-power curve) at the higher speeds—say at about 80% of the maximum speed of the locomotive. At what speed does the Deltic transmission unload, and does the author consider this unloading a disadvantage of the electric transmission when compared with a hydraulic transmission, which—it is claimed—can utilize the whole of the engine power up to maximum speed?

Mr. E. Cowin (*at Middlesbrough*): In view of the large amount of experience gained in the type of engine used in the Deltic locomotive, will this type of engine become popular with British Railways, who appear to be trying to find the best Diesel engine for their needs?

I note that the tank for the lubricating oil is an integral part of the underframe. What precautions are taken to prevent slag mixing with the oil, since during the final welding of the tank all the welds cannot be efficiently cleaned or ground?

The control gear on some electric traction equipment was locked up for a year and given no attention; after this period it was inspected, and since nothing was found to be wrong, it was locked up again for a further year. In view of this, would it be better not to proceed too far with Diesel-electric locomotives, and to install pure electric locomotives in their place, which would get rid of the troublesome Diesel engine and demand far less maintenance?

Mr. J. H. Addenbrooke also contributed to the discussion at Birmingham.

Mr. C. M. Cock (*in reply*): *To Mr. Brombley.*—The main generator can develop 1 200 kW at speeds up to 1 500 r.p.m., and its armature has a duplex lap winding.

The possibility of 'brinelling' is not confined to a stationary engine. It could arise with any type of locomotive having roller bearings for axles and auxiliary machines. I am not aware of any cases of 'brinelling' in idle bearings under conditions of vibration.

The auxiliary generators are not connected in parallel, each normally supplying part of the auxiliary load; but when only one power unit is being used, the auxiliary generator which is running automatically supplies all the auxiliary load.

Although, unfortunately, isolated cases of discharged batteries do occur, these have hitherto been considered insufficient justification for duplication of the battery. In Diesel locomotives of all kinds duplication would really amount to 'belt and braces' at the expense of very considerable cost and weight.

Automatic wheel-slip detection is justified because it permits heavier trains to be hauled. It also protects the traction motors from damage which might arise from excessive slip. Wheel-slip protection devices of various kinds are used on Diesel-electric, Diesel-hydraulic and electric locomotives.

To Mr. Tolley.—With electric or Diesel-electric locomotives it is yet to be proved that hammer has a more detrimental effect than with steam locomotives; in fact, the former are usually easier on the track than the latter, owing to their smooth even torque and lack of dynamic augment, combined with well-designed bogies and suitable springing. The type of power is not a dominant factor when considering the effect of a locomotive on the track; the problem is one of mechanical construction of the locomotive. Good riding qualities are desirable, and these are influenced by suitable design and springing; the axle load is also important.

To Mr. Davies.—Unloading of the engine is prevented in an electrical transmission by weakening the traction-motor fields. In hydraulic transmission it can be prevented by selecting an adequate number of gear ratios or by using more than one torque convertor.

To Mr. Cowin.—The advantages of the Deltic locomotive are postulated in the paper, and it is for railway authorities to decide, on their experience of reliability, operation and cost, as to its suitable employment in comparison with others.

After construction is completed the lubricating-oil tanks are thoroughly cleansed and cleared of any migratory objects which might be present, and, of course, the oil in use passes continuously through filters.

I suggest that the question of maintenance of Diesel engines is not of sufficient importance with good engines to influence a very major policy such as a choice between electric and Diesel-electric traction.

DISCUSSION ON 'INVESTIGATION OF POWER FOLLOW CURRENT PHENOMENA USING A SYNTHETIC POWER SOURCE'* AND 'THE IMPULSE INITIATION OF ARC DISCHARGES'†

NORTH MIDLAND CENTRE, AT LEEDS, 15TH DECEMBER, 1959

Dr. B. Salvage: There are two detailed points regarding the authors' experiments which I should like to mention. The first concerns the voltage to which the capacitor in the power circuit was charged. The authors have measured the highest voltage at which during five or ten successive impulses no power arc developed and the lowest voltage at which a power arc developed after each of five or ten impulses. Table 1 of paper 2708 S shows that there was quite an appreciable difference between

* ALSTON, L. L., and BRUCE, F. M.: Paper No. 2707 S, November, 1958 (see 106 A, p. 123).

† ALSTON, L. L.: Paper No. 2708 S, November, 1958 (see 106 A, p. 133).

these two voltages. Why have the authors adopted this procedure instead of measuring the voltage at which a power arc followed 50% of the applied impulses?

Secondly, electrode conditioning decreased the critical charging voltage with all metals except aluminium. Do the authors attribute the low initial voltage measured with aluminium electrodes to the presence of an oxide film on the aluminium surface? The formation of oxide dust on the electrodes caused a roughening of the surface and a reduction in the work function. Have the authors any information on the relative importance of these two factors in reducing the critical voltage?

Dr. L. L. Alston and **Professor F. M. Bruce** (*in reply*): The mean value of the critical voltage (Section 4.4, Paper 2707 S) corresponds to the 50% value mentioned by Dr. Salvage; this mean value is, in fact, given in the Table to which he refers, and could be repeated consistently. The procedure described in these papers was adopted because it gives the range of critical voltage values as well as the mean value and is more rapid than the method suggested by Dr. Salvage.

In reply to the second point, there was oxide on the aluminium electrodes throughout the experiments, not only initially. The determination of the relative importance of work function and surface roughness was not required for the derivation of our criteria, so that no data were obtained apart from those given in the papers.

DISCUSSION ON 'THE PERFORMANCE OF DISPLACEMENT GOVERNORS UNDER STEADY-STATE CONDITIONS'*

Dr. D. Broadbent (*communicated*): The consideration of steady-state errors of any governed system without reference to its transient performance should be undertaken with caution. If reduction of these errors is attempted by increasing the gain of the system, instability of the governor will surely result. For example, if the governed machine of Section 5 is considered in more detail, we can say that for $H = 6$ kW-sec per kW, the accelerating time of the set, T_a, will be 12·4 sec; i.e. it takes 12·4 sec, or 3 900 rad, for the 50 c/s set to reach base speed using base torque. The governor gain, K, is $1/2\pi$ p.u./rad, whence the characteristic equation describing the set's oscillatory mode is

$$T_a p^2 + Dp + K = 0$$

where D p.u./rad/rad is the damping torque/p.u. slip. The natural frequency, Ω, is

$\sqrt{(K/T_a)} = \sqrt{(0\cdot159/3\,900)} = 6\cdot47 \times 10^{-3}$ rad/rad $= 2\cdot04$ rad/sec.

The damping factor is $D/2\sqrt{(T_a K)} = 0\cdot06$ if a typical value $D = 3\cdot0$ is chosen.

This represents a very oscillatory system, and instability would exist in the practical situation where there would be time lags in both the steam header and the steam-valve servo-motor. For example, if these time lags were each one-third of a second, the loop transfer function of the governed machine would be

$$\frac{\dfrac{K}{D}}{p\left(\dfrac{T_a}{D}p + 1\right)(0\cdot33p + 1)^2} = \frac{16\cdot65}{p(4\cdot13p + 1)(0\cdot33p + 1)^2}$$

A Bode plot of this function reveals a negative phase margin of about 65°; the system is unstable. It could, of course, be stabilized by the method given in Reference 3 of the paper; however, K is limited to little more than $1/2\pi$ even so.

The authors' comment that the governor increases the electrical stiffness of the machine is certainly true; again, however, consideration of the practical situation is necessary. An alternator with normal reactance could be expected to deliver 1·0 p.u. load at an internal angle of about 30°; whence its synchronizing coefficient, P_s, is approximately 2·0 p.u./rad. Feasible governor gains of the order of 0·1 p.u./rad then do not help stiffness very materially in practice.

It will be seen, therefore, that the working point on the descending portion of the power-angle characteristic envisaged in Fig. 6 is most unlikely; for this to be so, K must be greater than 2·0 p.u./rad, which is impossible from dynamic considerations. A further result of this difference in coefficients is that the change in internal alternator angle following a load increment is small compared with the change in time-error angle α, and, on a first approximation, may be neglected.

On the other hand, system electrical angles can approach time-error angles in cases where a machine is connected through a high impedance (for example, a weak tie-line) to other machines, or where, as the authors point out in Section 3, isolated machines supply a local static load. In the latter case a multiple solution can be obtained if the governor produces power which is a linear function of angle and the machine produces power according to a sinusoidal function of angle. It is suggested, however, that in a large closely knit system (with dynamic loads and many of the prime movers operating on blocked governors) pole slipping will not occur for increments in load normally encountered, particularly if the governing stations have been wisely chosen.

Miniature system[A] and analogue computer studies[B] have shown that, where light tie-lines exist, the electrical angle must be watched closely and controllers which measure the tie power and tie angle must be installed. These controllers can be considered to rotate the reference vector, R, according to the time integral of these measure quantities, thus reducing deviations in steady state to zero. If machines are controlled within their limits no pole slipping occurs.

Finally, it should be said that there is no fundamental difference between a speed-governed system with supplementary frequency and time control, as widely exists, and a time-governed or displacement-governed system in which stabilizing velocity feedback is used. On the other hand, it has been shown[B] that the speed-governed system is, in general, inferior on the criterion of the integral square errors of load division and frequency.

References

(A) Broadbent, D.: 'Time Error Control for Interconnected Synchronous Electric Power Systems', *Transactions of the American I.E.E.*, 1959, **78**, Part III, p. 1554.

(B) Broadbent, D., and Stanton, K. N.: 'An Analytical Review of Power-System Frequency, Time and Tie-Line Control', *Proceedings I.E.E.*, Monograph No. 395 S, September, 1960 (**108 C**).

* Prescott, J. C., and El-Kharashi A. K.: Paper No. 3179 S, February, 1960 (see **107 A**, p. 85).

Dr. J. C. Prescott and **Dr. A. K. El-Kharashi** (*in reply*): The investigation which formed the basis of our paper was undertaken in order to fill what we believed to be a gap in the published information regarding displacement governors. As the title implies, we directed our attention to the performance of this type of governor when operating under steady-state conditions, and in order to make clear the essential characteristics, we assumed, when presenting numerical examples, values of the governor constants which would emphasize the difference between this type of control and other types in common use, e.g. the centrifugal governor.

In Section 5, which is not strictly relevant to the subject of the paper, we tried, by calculating the period of oscillation, to suggest that difficulties might occur if displacement governors, having comparatively large values of gain, were applied to turbo-alternator units which are generally assumed to have time lags in the region of 0·5 sec, attributable to header steam and steam-valve servo-motor.

Dr. Broadbent now carries the discussion further, making it more cogent. Taking two time lags of 0·33 sec, he shows that an isolated machine with a governor gain of $1/2\pi$ p.u./rad will in fact be unstable, in the absence of damping, artificially introduced.

The possibility of pole slipping will depend, as Dr. Broadbent suggests, upon the nature of the system considered. If fixed-throttle machines carry a portion of the load while the remainder is supplied by sets having displacement governors, then any increase in load demand will fall upon these latter and their vectors will fall back instantaneously with respect to the standard vector in order that their prime movers may increase their output. Whether this retardation is accomplished in synchronism or with pole slipping will depend upon the manner in which the load increment distributes itself between the sets and upon the electrical and dynamical constants of the sets themselves. If the sets are 'matched' in respect of their constants and load increments, synchronism is more likely to be maintained. In experimental work on small dissimilar sets supplying a dead load, we found that pole slipping was very general for an increase from half to full load. This is probably a larger amount than that contemplated by Dr. Broadbent, and perhaps in choosing 'wisely' between stations he would have in mind the desirability of 'matching'.

PAPERS AND MONOGRAPHS PUBLISHED INDIVIDUALLY

Summaries are given below of papers and monographs which have been published individually. The papers are free of charge; the price of the monographs is 2s. each (post free). Applications, quoting the serial numbers as well as the authors' names, and accompanied by a remittance where appropriate, should be addressed to the Secretary. For convenience, books of five vouchers, price 10s., can be supplied.

Short-Circuit Ratings for Mains Cables. Paper No. 3284 S.

G. S. Buckingham, B.Sc.(Eng.).

Research work carried out by the E.R.A. has enabled distribution engineers to assess with greater accuracy the strength of paper- (or cambric-) insulated cables to resist the forces arising from severe short-circuits.

E.R.A. Report Ref. F/T195 describes the research work which has been carried out over a number of years; the present paper interprets the results of the research and provides curves which illustrate the short-circuit currents which can be safely carried by such cables at working voltages up to 11 kV.

The Protection of High-Voltage Insulators from Power-Arc Damage. Paper No. 3289 S.

A. E. Guile, Ph.D., B.Sc.(Eng.).

The paper summarizes the results and conclusions of an experimental investigation into the reduction of damage by power-follow-through arcs on various types of high-voltage insulators. Where normally used, protective fittings of the usual type have been tested, and suggestions have been made for modifications to the fittings so as to give greater protection. The effects of wind have been taken into account. Only 11 and 33 kV insulators have been tested, but the investigation has been designed to obtain information which can be applied to the larger units at the highest voltages.

Since any application in service of these results must depend upon economic considerations, some information is included on the amount of arc damage occurring in service to high-voltage insulators, both in this country and abroad.

Radiocommunication in the Power Industry. Paper No. 3290 S.

E. H. Cox and R. E. Martin, D.F.H.

The paper reviews the present position with regard to the uses of radiocommunication by the electricity supply industry and draws attention to some of the problems which have been encountered in the planning and operation of mobile and fixed v.h.f. radio links. The paper deals with this subject from a general point of view, since the wide variety of applications and special requirements cannot be dealt with in detail in a single paper.

A Basis for Short-Circuit Ratings for Paper-Insulated Cables up to 11kV. Paper No. 3314 S.

L. Gosland, B.Sc., and R. G. Parr.

The various factors which may determine the short-circuit rating of paper-insulated cables are examined, and individual limits are proposed for each which may be decisive. These are the temperature rise of the conductors, the sheath temperature and the peak current. The application of these limits to a range of cables is illustrated.

A Brief Review of the Theory of Paper Lapping of a Single-Core High-Voltage Cable. Monograph No. 390 S.

P. Gazzana Priaroggia, Dr.Ing., E. Occhini, Dr.Ing., and N. Palmieri, Dr.Ing.

A brief outline of the theory of the lapping operation for high-voltage single-core paper-lapped cable is given, showing the stability conditions which the insulation thickness must satisfy in order that bending of the cable on the reel or capstan shall not cause any damage to the insulation itself.

An example of the design of a high-voltage cable is considered, showing the application of the theory outlined and its practical use.

Frequency Response Analysis of the Stabilizing Effect of a Synchronous Machine Damper. Monograph No. 393 S.

A. S. Aldred, M.Sc., Ph.D., and G. Shackshaft, B.Eng., Ph.D.

The paper describes the application of frequency-response concepts to the analysis of a synchronous machine damper in so far as it affects the stability of the machine. The analysis of damper effects is based on Park's equations. Small-displacement theory is introduced to organize the equations into the correct form for frequency-response computation.

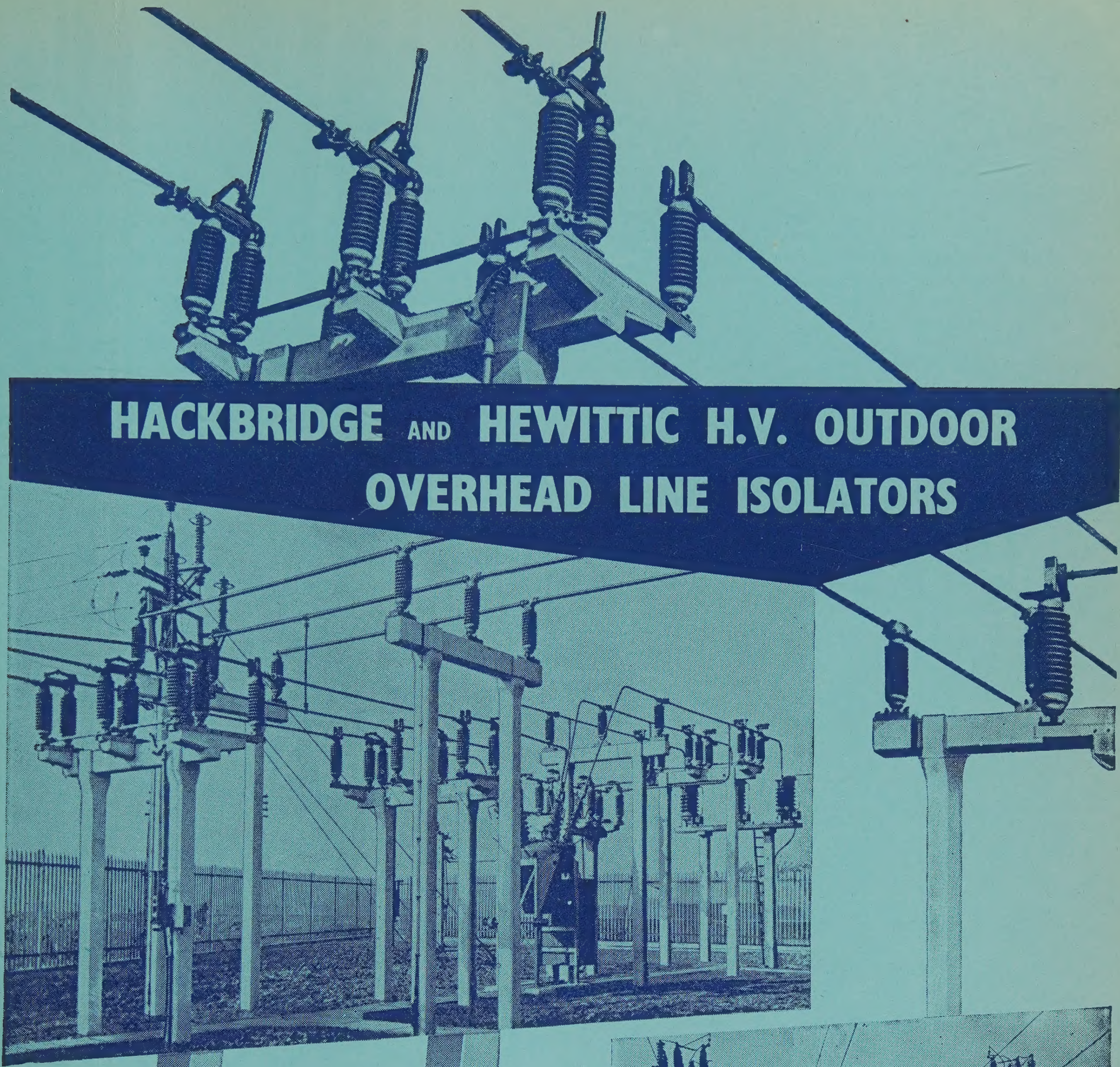
Nyquist diagrams are used to show the stabilizing effect of a damper and to illustrate the method of optimizing damper parameters.

The Magnetic Field and Centring Force of Displaced Ventilating Ducts in Machine Cores. Monograph No. 394 U.

K. J. Binns, B.Sc.

The paper examines analytically the field between equal stator and rotor ducts when displaced from each other and evaluates, for any relative position, the total flux crossing the air-gap and the amount entering the sides of each duct. The flux entering the sides of the stator ducts in a.c. machines varies at supply frequency and produces eddy currents whose path is not restricted by the direction of the laminations and which consequently give rise to considerable loss. With relative displacement of the ducts the fluxes entering the two sides of a duct become unequal and produce an axial magnetic force of engineering importance.

Numerical values are given for the variation of gap permeance and magnetic centring force, and are plotted in curves directly applicable to design calculation for ratios of duct to gap width varying from $\frac{1}{2}$ to 5 and for any relative displacement.



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Part A. POWER ENGINEERING, AUGUST 1960

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Example.—SMITH, J.: "Overhead Transmission Systems," *Proceedings I.E.E.*, Paper No. 4001 S, December, 1954 (102 A, p. 1234).

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